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Metallography of Carbon in Silicon–Iron Alloys Containing 4% Silicon

By E. D. Harry, B.Sc., A.I.M.

SYNOPSIS

In the first part of the paper an account is given of the mode of occurrence of carbon in 4·0% silicon–iron alloys used for the manufacture of transformer sheets. The account starts with the sheet bar, which may contain 0·050–0·10% of carbon, and ends with the finished sheet, which may have been annealed at temperatures ranging from 750–1150° C., and in which the carbon content may vary from below the limit of solid solubility to 0·030%.

Mention is made of an effect involving carbon segregation, which has been observed in this material. Pick-up of carbon from the furnace atmosphere during annealing is also described.

The second part of the paper deals with the constitution of the carbide phase in these silicon–irons, and it is shown that this phase is duplex in structure, consisting of the ordinary iron–carbide, Fe_3C, and what is believed to be an iron–silicon–carbon compound, the two carbides appearing to form a eutectoid.

Introduction

LITTLE work has been published on the metallography of hot-rolled sheets of 4·0% silicon–iron alloys for the making of transformer laminations. The composition of these alloys is simple, since the impurities are purposely kept to a minimum. A typical analysis is :

Si	3·8–4·2%
S	0·010–0·015%
P	0·010–0·03%
Mn	0·070–0·15%

Carbon is the most important impurity present, and a good sheet should not contain more than 0·01%. A study of samples taken at various stages during the manufacture of these sheets has revealed some interesting examples of various modes of occurrence of carbon.

Transformer sheets are usually 0·014 in. thick (29 gauge), about 36 in. wide, and 6–9 ft. in length. They are produced by hot rolling, beginning with a sheet bar about ½ in. thick, and the final rolling is carried out in packs of eight sheets. The process of sheet rolling employed has been described by Stanley,[1] and Fig. 22, which is a flow diagram of the process, is taken from his paper.

During rolling and annealing, efforts are made to reduce the carbon content to a figure as low as possible, without producing any accompanying ill effects. It will be seen from Fig. 22 that the rolling process involves four re-heats, and under correct conditions considerable decarburization can be effected at this stage.

In the following account, the products of the various stages of manufacture are considered in order.

MODE OF OCCURRENCE OF CARBON

SHEET BAR

The sheet bar is rolled from the ingot in one operation, that is, without any further re-heating after soaking the ingot. The carbon content of the bar generally lies between 0·05–0·10%. Carbon is present chiefly as pearlite, but it is usually accompanied by a small amount of carbide, in thin grain-boundary films. The pearlite can occur as elongated stringers or in a feathery form at the grain boundaries. It is not known precisely what controls the form that the pearlite will assume, but the soaking time of the ingot and the finishing temperature of rolling the bar are considered to be the chief factors.

Figure 1*a* shows elongated stringers of pearlite, and Fig. 2 the grain-boundary form. Figures 1*b* and *c* show both types of pearlite, highly magnified.

AS-ROLLED SHEET

Decarburization takes place during the rolling of the sheet, and the form in which the carbon occurs in the as-rolled sheet will depend on the amount of residual carbon. If the carbon content of the sheet

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Mr. Harry is Research Metallurgist at the Steel Company of Wales (Lysaght Works) Ltd., Newport.

is more than about 0·020%, pearlite is present as elongated stringers, accompanied by thin carbide films at the grain boundaries, as illustrated in Fig. 3. The pearlite has a very fine structure because of the rapid rate of cooling, and it is difficult to resolve.

Sheets containing less than about 0·020% of carbon do not contain pearlite, and in such cases the carbon is in the form of globules of carbide and grain-boundary films, and in an acicular form precipitated within the grains. The proportion of each form present under fixed cooling conditions is determined by the final grain size, a large grain size favouring the precipitation of the acicular form, and a small grain size enabling more of the carbide to diffuse to the grain boundaries.

Figure 4 shows grain-boundary films and globules, and acicular carbide, in an as-rolled sheet.

ANNEALED SHEET

The annealing treatment given to transformer sheets is of two main types, low-temperature annealing, at temperatures of about 750–850° C., and high-temperature annealing, at temperatures of over 1000° C. The carbon content of the annealed sheet should be less than 0·015%, and in many cases carbon contents of less than 0·005% have been recorded, though occasionally abnormal samples with carbon contents as high as 0·04% have been obtained. The very low carbon contents were determined by means of an apparatus developed in this laboratory, and based on Yensen's apparatus.[2]

Low-Temperature Annealed Sheet

Low-temperature annealing is carried out at a temperature of 750–800° C., and the cycle, which includes heating and soaking, occupies about 60 hr. The stack of sheets cools in about 90 hr. to a temperature at which it can be unpacked. During this annealing treatment an atmosphere of the partially burnt town's gas type surrounds the charge.

In sheets which have undergone low-temperature annealing, carbon is found in two forms, grain-boundary films and globules, and graphite. When the carbon content of this type of sheet is more than about 0·012%, graphite is invariably present, and very little, if any, carbide is found. If the carbon content of a sample of sheet annealed at 800° C. is less than 0·006%, no free carbide can be detected. Within the range 0·007–0·012% of carbon, a fairly accurate estimate of the carbon content can be obtained by careful microscopic examination. Figure 5 shows an example of heavy grain-boundary carbide films in a sheet annealed at 800° C. The carbon content of the sample from which this photomicrograph was taken was 0·012%. Examples of graphite in low-temperature annealed sheets are given in Fig. 6.

High-Temperature Annealed Sheet

High-temperature annealing is carried out at 1150–1200° C., and the complete cycle of heating, soaking, and cooling occupies approximately 7 days. During this cycle the charge is surrounded by an atmosphere of hydrogen or of a hydrogen and nitrogen mixture. Sheets which have been given this annealing treatment have a large grain size, so that very often the whole thickness of the sheet is covered by one grain. Such sheets usually contain about 0·01% or less of carbon, present as occasional thin films and globules at the grain boundaries.

Samples containing up to 0·035% of carbon, annealed at 1150° C. under the conditions described above, showed some very interesting carbide formations. As the carbon content increased from 0·01%, the grain-boundary films became thicker, then massive particles and globules of carbide appeared within the grain, and, in samples containing about 0·035% of carbon, occasional massive areas of pearlite were present. Figures 7 and 8 show some of these heavy grain-boundary films of carbide, whilst Fig. 9 shows an area of pearlite.

MIGRATION OF CARBON TO THE SHEET SURFACE

It has been found that under certain conditions the carbon in 4·0% silicon–iron sheets has a marked tendency to segregate at the surface. The effect was shown strikingly in a sample that had been annealed at 1100° C. in a slightly defective vacuum (about 1 cm. of mercury). The sample was a piece of as-rolled sheet, containing 0·035% of carbon which had been thoroughly pickled, and the cooling time from the annealing temperature was approximately 1½ hr. Microscopic examination showed that the carbon, in the form of pearlite, had concentrated near the surface of the sheet. This is illustrated in Figs. 10 and 11. The effect is thought to occur in the following way.

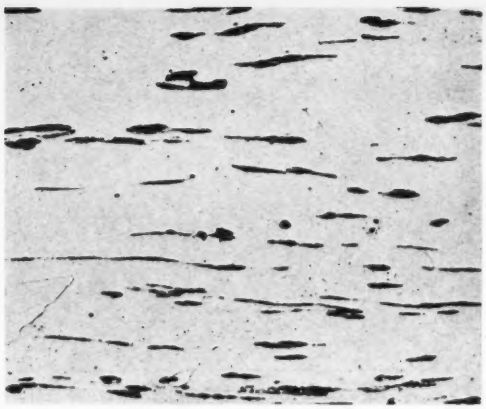
Because of the slightly defective vacuum, mildly oxidizing conditions prevailed during the annealing of the sample. Under these conditions some silicon near the surface of the sheet may have been oxidized in preference to the carbon, forming a sub-scale of silica and thus producing a thin film low in silicon. Carbon diffusing from the body of the sheet towards that part of the surface low in silicon became concentrated there, forming pearlite during cooling. It is not known why carbon should diffuse into areas low in silicon, but a parallel may be drawn from the effect of phosphorus banding in mild steel, in which carbon concentrates in the areas low in phosphorus.

Darken[3] has reported some very interesting experiments in which he homogeneously welded together two rods of differing composition and then annealed them for long periods at a temperature of 1050° C. He found that carbon diffused from a high-silicon austenite to a low-silicon austenite, even though the carbon content of the lower-silicon austenite was the greater.

4·0% silicon steels containing about 0·05% of carbon should be ferritic at all temperatures, although some traces of pearlite have been found after annealing at temperatures around 1100° C. Darken's results refer to diffusion of carbon in austenite, but the same results may hold for diffusion in silicon ferrite.

A somewhat similar phenomenon has also been observed by Ihrig[4] while working on the siliconization of steels. He found that the carbon in the original steel diffused from the area of high silicon content, and concentrated as a band at the junction of the siliconized surface film and the original steel.

Mr. H. H. Stanley has informed the author in a private communication that while examining sheet



(a) × 150

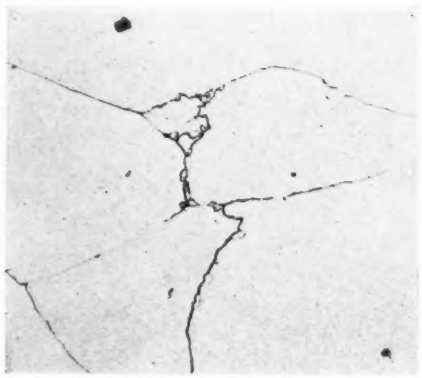
(b) × 1200

(c) × 1200

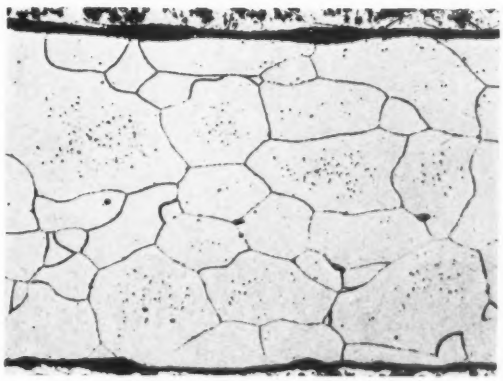
Fig. 1—Pearlite in 4% silicon sheet bar : (*a*) Elongated stringers. Etched with 2½% nital ;
(*b*) as in (*a*), resolved. Etched with 4% picral ;
(*c*) feathery type, resolved. Etched electrolytically with alkaline sodium picrate

Fig. 2—Feathery grain-boundary pearlite in 4% silicon sheet bar. Etched with 2½% nital × 150

Fig. 3—Central pearlite stringers in as-rolled sheet. Etched with 2½% nital × 150



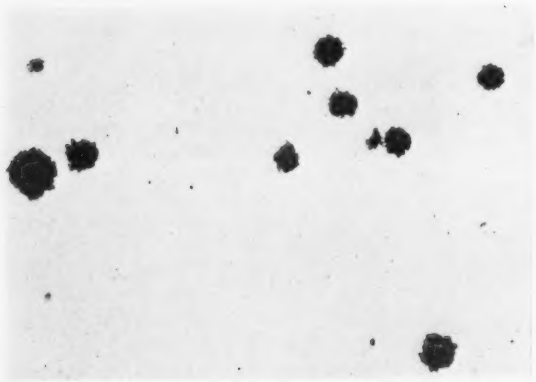
(a) × 250



(b) × 150

Fig. 4—Carbide in as-rolled sheet : (*a*) Grain-boundary films and globules. Etched with 4% picral × 250 ; (*b*) precipitation of acicular type. Etched with 2½% nital × 150

Fig. 5—Grain-boundary films and lakes of carbide in low-temperature-annealed sheet. Etched with 4% picral × 250



(a)
× 550

(b)
× 1200

Fig. 6—Graphite Nodules : (*a*) Cluster in low-temperature-annealed sheet. Unetched × 550 ; (*b*) Nodule. Unetched × 1200

Fig. 7—Heavy continuous grain-boundary carbide films in high-temperature-annealed sheet. Etched with 2½% nital × 250

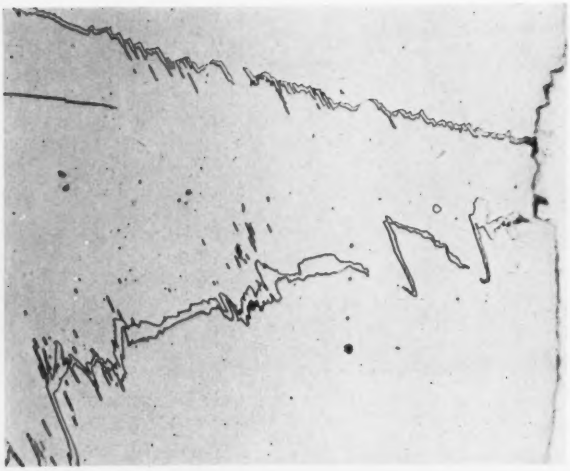


Fig. 8—Exceptionally heavy grain-boundary carbide film in high-temperature-annealed sheet. Etched with 4% picral × 250

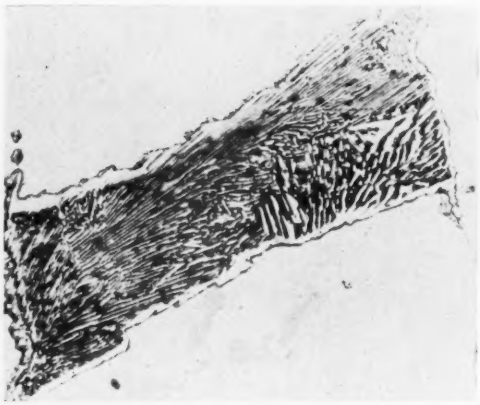


Fig. 9—Area of pearlite in high-temperature-annealed sheet. Etched with 4% picral × 550

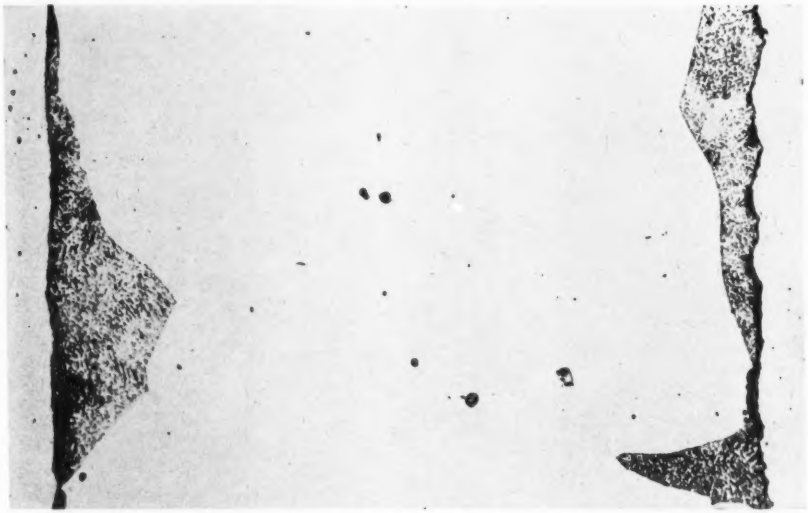


Fig. 10—Pearlite at surface of sheet annealed at 1100° C. in very slightly oxidizing atmosphere. Etched with 2½% nital × 250

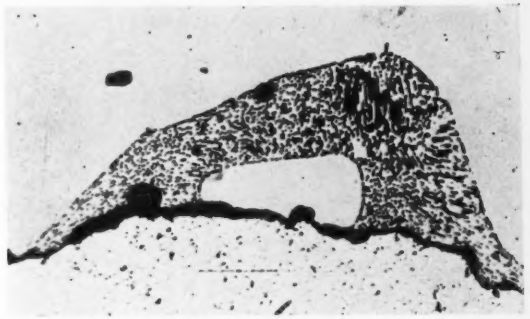


Fig. 11—As in Fig. 10. Etched with 4% picral × 550

(a) (b)

Fig. 12—Carbon picked up from furnace atmosphere during annealing at 800° C. : (*a*) Grain-boundary films. Electrolytically etched with alkaline sodium picrate ; (*b*) Continuous film of carbide on surface of sheet. Etched with 4% picral × 1200

Fig. 13—Surface pearlite, formed by pick-up of carbon from furnace atmosphere during annealing at 950° C. Heat tinted without pre-etching × 1200

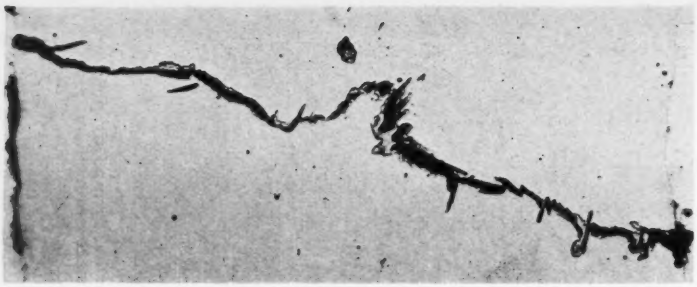


Fig. 14—Heavy carbide film. Etched lightly with picral and heat tinted × 250

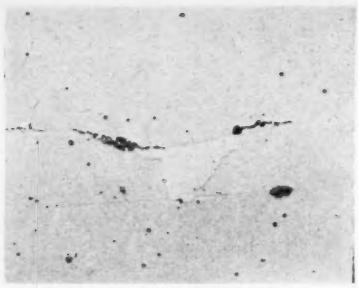


Fig. 18—Carbides in low-carbon sheet (0·009%) showing areas affected and unaffected by heat tinting. Etched lightly and heat tinted × 1200

Fig. 15

Fig. 19

Fig. 16

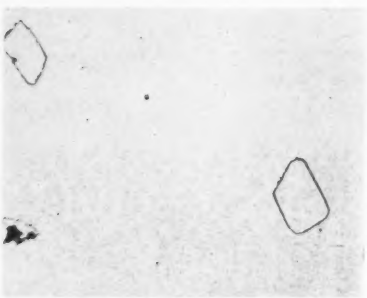


Fig. 20

Fig. 15—Carbide film etched lightly with picral, and heat tinted × 550

Fig. 16—Carbide lake and film. Etched lightly and heat tinted × 1200

Fig. 19—Heat-affected carbide area in low-carbon sheet resolved into duplex structure. Heat tinted after preliminary etching × 2800

Fig. 20—Crystalline carbides in 4% silicon bar annealed in laboratory furnace at 1200° C. and allowed to cool in furnace. Etched with 2% nital × 550

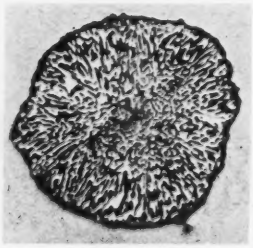


Fig. 17—Carbide globule within a grain. Etched lightly with picral and heat tinted. × 1200

(a) (b)

Fig. 21—Crystalline carbide in 4% silicon bar annealed at 1200° C. : (*a*) Carbide only slightly affected. Etched with picral and heat tinted ; (*b*) partial break-down of the solid solution into duplex constituent. Etched with picral and heat tinted × 2800

bars of 4·0% silicon steel, he found that one sample contained the mild-steel splash plate which is placed on the mould stool to prevent wear by the stream of liquid steel. The splash plate had not been melted completely, but had formed a core to the silicon steel bar. The remarkable feature was that the silicon ferrite near the splash plate was practically free from carbon, whereas the splash plate had a high carbon content.

PICK-UP OF CARBON FROM THE FURNACE ATMOSPHERE

Sheets of 4% silicon–iron in the pickled condition will pick up carbon during annealing from certain atmospheres of the partially burnt town gas type. When this happens during annealing at 800° C., the carbon has been found to remain on the surface as films of carbide. Typical examples of such surface films are shown in Fig. 12.

When pick-up of carbon occurs during annealing at 950° C., pearlite is formed at the surface of the sheet, as shown in Fig. 13.

DISCUSSION

Very little information has been published on the microstructure of 4·0% silicon–iron alloys containing small quantities of carbon, of the order of 0·05% or less. Rickett and Fick[5] determined experimentally the constitution and microstructures of a series of commercial silicon steels, over the temperature range 700–1370° C. The diagram which they plotted from their results is reproduced in Fig. 23. It deviates to some extent from Yensen's diagram[6] (Fig. 24), which predicts that all alloys containing even as much as 0·10% of carbon will be entirely ferritic at all temperatures if the silicon content exceeds 3·5%. Rickett and Fick's diagram shows that an alloy containing 4·25% of silicon and 0·05–0·08% of carbon consists of 10% austenite and 90% ferrite at 1090° C. In regard to the mode of occurrence of carbon in annealed iron–silicon–carbon alloys containing 4·0% silicon, Yensen[7] found that in 4–6% silicon alloys, with a carbon content of 0·005–0·05%, the carbon was precipitated in part as Fe_3C and in part as graphite. With a carbon content exceeding 0·05%, the carbon was precipitated solely as graphite. Yensen's annealing method consisted of heat-treatment at 900–1400° C. in hydrogen or a vacuum, with slow cooling at the rate of 30° C. per hour. The observations reported in this paper show that after a commercial annealing at 1050–1150° C., followed by cooling at the rate of 10° C. per hour to 600° C., no graphite was formed even when the carbon content was 0·05%.

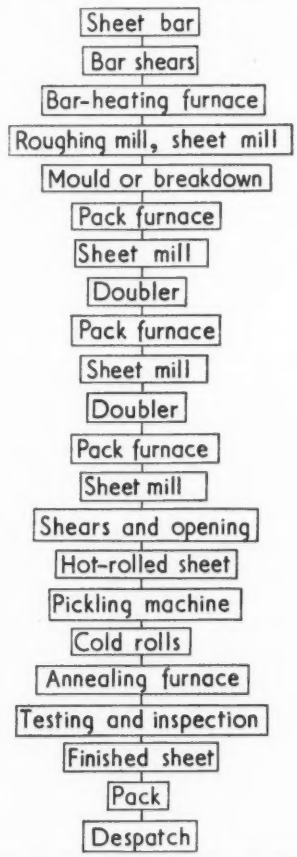


Fig. 22—Flow diagram for sheet bar to finished sheet. (29G sheet by hand mill process)

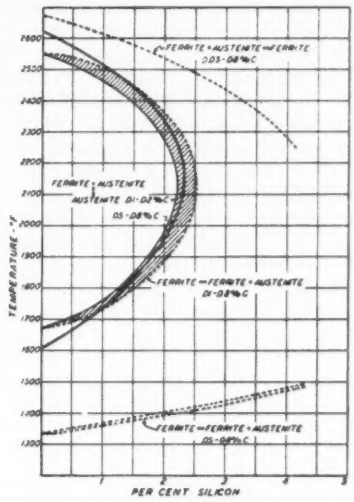


Fig. 23—Constitution diagram of iron–silicon alloys with 0·01–0·02% and 0·05–0·08% carbon

It should be emphasized, however, that Rickett and Fick's experiments and the observations reported in this paper were made on commercial steels, whereas Yensen's diagram was based on work carried out on pure alloys, prepared in a vacuum furnace from electrolytic iron and pure silicon.

It was mentioned before that graphite was found in low-temperature annealed sheets in which the final carbon content was above about 0·012%, whereas as much as 0·05% of carbon was present entirely as carbide in material which had been annealed at a high temperature, although the cooling conditions from 800° C. were practically the same for

both treatments. There was no obvious explanation of the difference in the final form of the carbon.

After some experimental work, it was found that graphite was formed in low-temperature annealed sheets only if the as-rolled or 'hard' sheet contained pearlite. During annealing below Ac_1 the pearlite became graphitized, the graphite remaining out of solution. It was found in practice that only comparatively short annealing times at 800° C. were required to graphitize the pearlite in the as-rolled sheet. At an annealing temperature of 1150° C., the pearlite formed austenite. Even if graphitization of the pearlite occurred during heating up, the graphite would dissolve in the silicon ferrite at the subsequent high temperatures.

THE CONSTITUTION OF THE CARBIDE PHASE

EARLY WORK

A great deal of work has been published on the iron–silicon–carbon system, but practically all of it has been concerned with alloys of composition resembling the cast irons, and therefore very considerably higher in carbon than the material considered here.

Gontermann,[8] Stead,[9] and Honda and Murakami[10] have published work dealing with the constitution of the carbide phase in iron–silicon–carbon alloys high in carbon. They reported that two types of carbide are present; one the ordinary iron carbide, Fe_3C, and the other a silico-iron–carbide. Stead distinguished between the two by heat-tinting; the Fe_3C was darkened in the usual manner, and the silico-carbide remained unaffected. He obtained some excellent photomicrographs, in one of which the two carbides appeared in a eutectoid-like structure. Honda and Murakami stated that the ordinary iron carbide was not found if the silicon content exceeded 5·5%, and that the two carbides were mutually insoluble at room temperature. Kříž and Pobořil[11] found that two carbides existed in iron–silicon–carbon alloys, one of which was coloured by alkaline sodium picrate and the other of which was not. No indication was given of the conditions required for the formation of the two carbides.

Hurst and Riley[12] have described the carbide phase in silicon irons of the acid-resisting type containing 10–15% of silicon. They found that a small quantity of the carbide was present in commercial castings, and that the quantity increased with the rate of cooling. This carbide was not blackened by boiling alkaline sodium picrate, and its stability was low; annealing for 1–2 hr. at 800° C. was sufficient to cause it to decompose into graphite. The authors suggested that the formation of this carbide might be related to the presence of manganese and phosphorus. More recently Marles[13] has added a further contribution on the subject. This author studied the carbides present in cast irons and iron–silicon–carbon alloys which contained up to 7·0% of silicon and from 1·7 to 3·6% of carbon. With a silicon content of more than 2·5% he found a carbide phase which did not respond in the same way to heat-tinting as did cementite, which was also present.

In the literature referred to, no mention has been made of alloys containing approximately 4·0% of silicon and 0·05% of carbon, nor has any indication been found whether the carbide phase in alloys of this type consists of Fe_3C, a silico-carbide, or a mixture of the two. Only Yensen[7] states specifically that the carbide phase in this type of alloy is Fe_3C.

EXAMINATION OF HIGH-TEMPERATURE ANNEALED SAMPLES

Some high-temperature annealed samples containing 0·03% of carbon provided an excellent opportunity to study the nature of the carbide present. The grain size of these samples was large, and on the average one grain covered the thickness of the sheet. The carbide phase occurred as very heavy grain-boundary films and as fairly large globules within the grains.

Examination by polarized light did not reveal any differences between various parts of the carbide phase, but, when the samples were etched in picral and then heat-tinted, it was found that not all the carbide was affected to the same extent. Most of the carbide in the heavy carbide film was affected in the same way as cementite; the remainder was affected to a slightly lesser degree. The effect is shown in Figs. 14 and 15 which show heavy grain-boundary films of carbide after heat-tinting. The majority of the carbide phase has been markedly affected, while small areas have been affected to a lesser extent.

Examination of the lightly affected areas, at a magnification of × 1200, showed that they were duplex in structure, composed of two constituents, one of which had been strongly affected by heat-tinting, and the other not at all. The carbide globules at the grain boundaries and within the grains were also duplex in structure; this is illustrated in Figs. 16 and 17.

To confirm the results obtained by heat-tinting, the samples were etched with boiling alkaline sodium

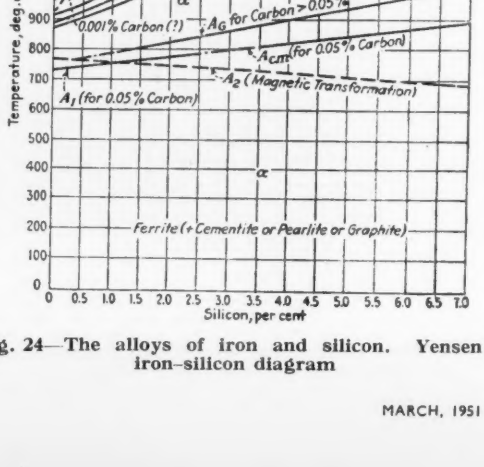


Fig. 24—The alloys of iron and silicon. Yensen's iron–silicon diagram

picrate. Similar effects were obtained, although the clarity of the duplex structure was not so pronounced because of staining and level differences between the two types of carbide after etching. Electrolytic etching using alkaline sodium picrate gave better results than were obtained with the boiling reagent.

Results obtained from the examination of the carbide phase in a number of samples of material, which had been commercially annealed at a high temperature, and which had carbon contents ranging from 0·009–0·035%, may be summarized as follows :

(1) Carbon contents of less than about 0·012%—no free carbide (of the type which behaved like cementite on heat-tinting) present as a separate phase

(2) Carbon contents of more than about 0·012%—Carbide which behaved like Fe_3C present as a separate phase, together with the duplex phase.

EXAMINATION OF LOW-TEMPERATURE ANNEALED SAMPLES

Examination of 4·0% silicon sheets commercially annealed at low temperatures showed that the amount of carbide phase produced by, say, 0·01% carbon, seemed to be rather more than would be expected if a plain carbon steel containing a similar amount of carbon is taken as a basis of comparison. The carbon content of suitable samples which had been given the commercial low-temperature annealing treatment at 800° C. was always less than about 0·012%, since graphite is present when the content exceeds this figure. The grain size also was small compared with the high-temperature annealed material, and because of these two factors, there were few areas of carbide of sufficient size to allow examination. Samples examined at a magnification of × 1200 after heat-tinting showed that :

(*a*) The greater proportion of the carbide consisted of a phase which was affected by heat-tinting, though not quite to the same extent as cementite

(*b*) A small amount of a carbide phase which was completely unaffected by heat-tinting was present ; this phase showed a tendency to form idiomorphic crystals.

Figure 18 shows areas of the two phases. The areas affected by heat-tinting when observed at still higher magnifications were seen to be of duplex structure, as was the phase found in the high-temperature annealed samples, but with a much finer structure. Figure 19 shows one of these areas.

EXAMINATION OF A LABORATORY-ANNEALED SAMPLE

A small piece of 4% silicon steel sheet bar was annealed for 3 hr. at 1050° C. in a small laboratory furnace. The sample was allowed to cool with the furnace at a rate such that the temperature had fallen to 130° C. in about 2 hr. The sample, when examined, showed carbide present in part as thin grain-boundary films and in part as particles within the grains, the latter showing a very marked idiomorphic outline, as illustrated in Fig. 20. When the sample was heat-tinted, the idiomorphic carbide phase was affected only very slightly. Examination at the highest magnification obtainable failed to reveal any duplex structure in the majority of the carbide particles and films. Figure 21*a* shows one of these homogeneous carbide particles after heat-tinting.

Though most of the carbide particles appeared homogeneous after heat-tinting, some small particles of the duplex phase, and one or two very interesting crystals in which formation of the duplex phase had begun, but was not complete, were seen. This is illustrated in Fig. 21*b*. The carbide at the grain boundaries, which must have been the first to come out of solution, appeared to contain more of the duplex phase than the particles within the grain.

The idiomorphic crystals which comprise the greater part of the carbide phase in the sample must be a solid solution of the two carbides which are found as separate phases, and as a eutectoid phase, in slowly cooled samples. It is considered that the two carbides are Fe_3C, which is strongly affected by heat-tinting, and an iron–silicon carbide, which is not. The comparatively rapid rate of cooling of this laboratory-annealed sample prevented the eutectoid reaction from taking place completely, and the solid solution persisted at room temperature.

PEARLITE IN 4·0% SILICON ALLOYS

The carbide in the pearlite found in as-rolled sheets does not respond to heat-tinting as markedly as does Fe_3C. The as-rolled sheets cool comparatively rapidly from the rolling temperature, and the ease with which graphite can be formed in samples containing pearlite has been mentioned. These facts suggest that the carbide part of the pearlite is not Fe_3C, but probably a solid solution of Fe_3C and the silico-carbide : evidence that such a solid solution of the two carbides is readily graphitized is given in the next section.

RELATIVE STABILITY OF THE TWO CARBIDES

It was not possible to carry out graphitizing tests on samples containing both types of carbide, because the carbon content of the available samples was within the solid solubility limit at temperatures likely to produce decomposition. Tests on samples of sheet bar containing 0·08% of carbon did, however, enable some information on the graphitization of the carbides to be obtained.

A sample of this material was annealed at 1050° C., and allowed to cool with the furnace in about 2 hr. The carbides were found to be of the type described above, a solid solution of cementite and silico-carbide. The sample was then annealed at 750° C. for 1 hr., and a proportion of the carbon was then present as graphite : the carbide in excess of the solubility limit at 750° C. appeared, therefore, to have decomposed. It would seem that a carbide which is apparently a solid solution of cementite and the silico-carbide is markedly unstable, and readily decomposes to graphite.

CONCLUSIONS

The term ' silico-carbide ' has been used to describe the phase not responsive to heat-tinting, as was originally found by Stead.[9] It is considered that this silico-carbide is a definite compound of iron, silicon, and carbon, in agreement with the observations of Marles.[13]

The results and observations presented in this paper may be summarized as follows :

(1) Two carbides have been found in silicon–iron alloys containing 4·0% of silicon and less than 0·05% of carbon, when the material was commercially annealed. This annealing treatment should give rise to a state very nearly approaching equilibrium.

(2) The two carbides may be distinguished by their response to heat-tinting, to etching with boiling alkaline sodium picrate, or to electrolytic etching with the same reagent. The carbide not affected by heat-tinting showed a marked tendency to form idiomorphic crystals.

(3) The two carbides have been found as separate phases, and as a duplex constituent with a eutectoid-like structure.

(4) With samples of carbon content higher than about 0·012%, cementite and the eutectoid-like phase were present. Samples with carbon contents lower than about 0·012% contained crystals of silico-carbide and the eutectoid-like phase.

(5) The two carbides appeared to be completely soluble in each other at high temperatures, but at room temperature were completely insoluble or showed only a limited mutual solubility.

(6) The carbides appeared to undergo a eutectoid reaction at a temperature of about 400° C. The eutectoid composition appears to correspond to a carbon content of about 0·012% in the alloy.

Acknowledgments

The author wishes to thank Mr. H. H. Stanley, Chief Metallurgist, The Steel Company of Wales (Lysaght Works) Limited, for his advice and encouragement, and also the Directors of Lysaght Works for permission to publish this paper.

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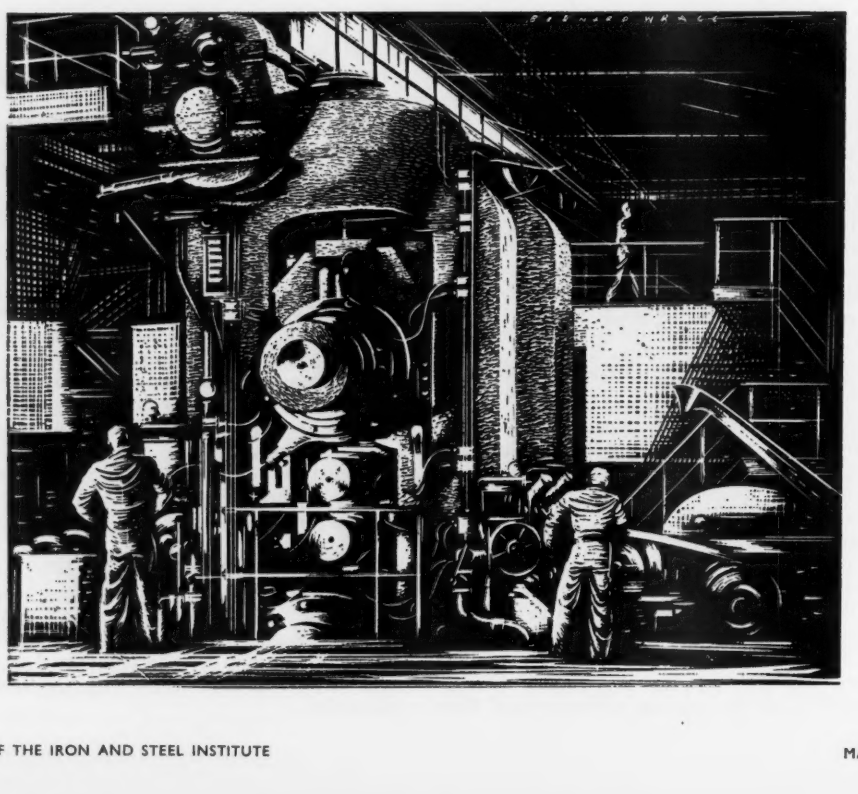

The Variation in Electrical Properties of Silicon–Iron Transformer Sheet

A STATISTICAL ANALYSIS OF DATA FROM A SINGLE CAST

By S. Rushton, M.A., B.Sc., D.I.C., and D. R. G. Davies, B.Sc., A.I.M.

SYNOPSIS

This study of the variation in iron-loss values of sheets produced from a single cast of silicon–iron transformer sheet is based on 264 values from 11 ingots of the cast, and on iron-loss values of all the sheets produced from one particular ingot. The statistical technique of analysis of variance has been used to show the contributions to the total variation among these values due to differences between ingots, variation due to different positions in an ingot, and variation due to differences in the position of the sheet in the pack at the sheet-rolling stage. The effect of a second anneal has been studied, using the analysis of covariance technique. Causes of the variation in iron-losses are discussed.

In 1903 the development of low-carbon silicon–iron non-ageing sheet marked a great advance in the quality of materials suitable for transformer cores.[1] There has since been a steady, but not spectacular, improvement in the quality of silicon–iron sheet, which is still supreme among core materials used in the construction of transformers and heavy electrical plant. Whilst great progress has been made in the development of anisotropic silicon–iron strip possessing excellent electrical properties,[2, 3] there is a continued demand for high-grade ordinary hot-rolled silicon–iron sheets, and the work recorded in this paper was undertaken in an endeavour to improve this latter class of material.

The present requirements of transformer sheets have been reviewed in detail by Brailsford,[2] and are, briefly : (i) high permeability, (ii) low total iron-loss at a frequency of 50 cycles/sec. and flux densities of 10,000–15,000 gauss, and (iii) high space factor. Improvement of the space factor involves special study at the sheet-rolling stage, but the problem of reducing iron-losses entails careful consideration of every stage of manufacture, from the selection of scrap for melting to the final processes. In general the sheet manufacturer does not carry out regular tests for permeability ; he relies on regular iron-loss tests and occasional space-factor tests for the control of quality.

The primary aim in producing transformer sheets is to obtain low iron-loss on the average, but it is equally important that variation in iron-losses within a single cast, and further, from cast to cast, is as small as possible. This latter variation, between the casts produced over a long period of manufacture, has been the subject of an extensive investigation ; the data have yet to be examined by precise statistical methods. The present paper deals with the first type of variation, that is, in a single cast. To estimate the variation in iron-losses within a single cast and to form some idea of the possible causes, a particular cast of silicon–iron sheet was made the subject of special study. Observations on certain concomitant factors, such as the rate of teeming, the teeming temperature, and the ingot-rolling temperature, were made at the various stages of processing. A complicated system of tallies and marks was operated so that the precise history of the final sheet products could be traced through every stage of manufacture, from the teeming of the ingots to the testing of the finished sheets.

It will be seen later that in this particular cast the average iron-loss at 50 cycles/sec. and 10,000 gauss was 1·17 W./kg. This cast cannot be regarded as being of high quality, since sheet with an average iron-loss of 1·0 W./kg. is regularly produced and there is an appreciable output of 0·9 W./kg. material, particularly in America.* This latter grade of sheet was also produced in Germany just prior to World War II.

The present study of a particular cast is concerned more with variability than with the actual level of quality ; these features tend to be closely related and techniques giving a higher average level of quality are likely to show smaller variability. The cast studied was made in 1947 ; since then techniques have been modified and the general level of quality has improved, but, in spite of this, the study of variability and its possible causes is still relevant.

DESCRIPTION OF PROCESSES AND PROCEDURE FOR STUDYING THE CAST

Apart from additional observations on concomitant variables, the adoption of special means of tracing the history of each sheet produced from the cast, and the carrying out of iron-loss tests on an unusually

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Mr. Rushton is at Imperial College, London, and Mr. Davies, Chief Metallurgist at the Redbourn Works of Richard Thomas and Baldwins, Ltd., was also at Imperial College when this work was undertaken.

* In Britain there is at present a considerable output of 0·9–1·0 W./kg. material from open-hearth furnaces, using a ladle mixing technique. This steel contains 4% of silicon and 0·5% of aluminium.

Table I

DATA RECORDED DURING CASTING AND ROLLING

Notes

(1) All moulds were tarred while hot, prior to teeming.
(2) Teeming was clean and casting was good until mould 5 was three-quarters full, when the burner had to be used to clear the nozzle, causing 25 sec. delay in teeming this ingot. The burner was used again when mould 12 was almost full. Mould 13 produced only one-third of an ingot.
(3) Stripping of the moulds presented no difficulty except for No. 1, when there was sticking trouble. With most ingots there was a little difficulty in removing bottom plugs.
(4) Diameter of the ladle nozzle before teeming was 1¼ in., and after all teeming 2⅛ in. approx.
(5) On completion of teeming, very little skull was left in the ladle.

Mould No. (Ingot No.)	Casting-Pit Observations		Rolling Ingots into Sheet Bars	
	Teeming Temp., °C.	Time to Fill Mould, sec.	Initial Rolling Temp., °C.	Finishing Temp., °C.
1	1580	39	...	...
2	1540	46	1080	835
3	1540	60	1080	830
4	1540	66	1050	830
5	1530	83	1060	820
6	1540	50	1050	840
7	1530	55	1080	840
8	1530	50	1060	820
9	1520	67	1060	830
10	1500	78	1060	830
11	1500	95	1060	830
12	1480	131	1085	880
13	1460	...	...	...

large number of samples, the processing of the cast selected for study was entirely normal. A brief outline of the process follows.

Melting

Melting was carried out in a 12-ton electric-arc furnace, the charge consisting of specially selected plain carbon steel scrap, part in slab form and part in bundles of light scrap. The carbon, phosphorus, silicon, and manganese were reduced to low values under an oxidizing slag. This was later replaced by a reducing lime slag which reduced the sulphur content and made the bath suitable for receiving without undue loss from oxidation, enough ferro-silicon to produce about 4% of silicon in the charge.

Casting Pit

When the ferro-silicon had been adequately mixed into the charge, the slag and temperature were adjusted, and the furnace was tapped into a ladle, which was 'held' to allow flotation of non-metallic inclusions before teeming into 13 tarred 18¾-cwt. moulds of the wide-end-up type. The moulds were marked from 1 to 13 in order of teeming, and the temperature and teeming time for each mould were recorded; certain other observations were also made (Table I). Temperatures were measured with a disappearing-filament pyrometer, the reading being taken on the metal stream towards the end of pouring to minimize interference from tar fumes. The time taken to reach the level of the hot-top mould junction was used as a measure of the pouring rate. Ingot 1 was delayed in the pit owing to stripping difficulties. Ingot 13 was only one-third of the full size and was returned for scrap. Ingots 1 and 13 were thus eliminated. Ingot 2 is of special importance since the sheet product from this ingot was examined in much greater detail than that from any of the other ingots; practically all the sheets from ingot 2 were tested for iron-loss, whereas for the other ingots iron-loss tests were carried out only on the product from the top, middle, and bottom of each ingot. When ingots 2 to 12 had cooled to 640–680° C. they were stripped from the moulds and were charged immediately into soaking pits fired with producer gas, charging being completed within 15 min.

Rolling Ingots into Bars

After 3½ hr. in the soaking pits the ingots were at a temperature suitable for rolling into sheet bars, this being carried out without intermediate heating in a reversing bar mill. After the first pass, which breaks the scale, an optical temperature reading was made on each ingot. After the final pass a 'finishing' temperature was also recorded (Table I).

On leaving the mill each ingot was in the form of a long bar, which was sheared while hot into sheet bars, approximately 38 in. long and about 15 lb. 2 oz. per ft. in weight; these dimensions were suitable for subsequent rolling into sheets 72 × 36 × 0·014 in. During shearing, those parts corresponding to ingot heads were discarded and no bars were accepted if seen to contain 'pipe' or surface slivers. At this stage all sound sheet bars from ingot 2 and two sound bars each from the top, middle, and bottom of ingots 3 to 12 were assembled in readiness for sheet rolling. The remaining bars from the cast were placed in stock and were excluded from the subsequent scheme of detailed observation.

Sheet Rolling

Sheet bars were cold on arrival at the sheet mill. Reduction to sheets of 72 × 36 × 0·014 in. involved three heatings. The rolling procedure was as follows:

Sheet bars soaked for 80 min. in producer-gas-fired furnace at 1020–1040° C.
Each bar given three passes in a two-high mill.
Two bars matched, given three passes, and then doubled.
The doubled product heated for 10 min. in a coal-fired steam-injected furnace at 980–1010° C.; given one 'run-over' pass, doubled and reheated in a coal-fired steam-injected furnace at 990–1020° C.; then given three finishing passes, sheared, opened, and allowed to cool.

The progress of a pair of bars during rolling is represented diagrammatically in Fig. 1. Although

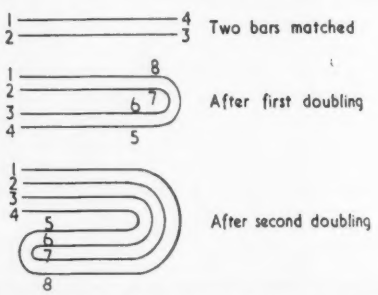

Fig. 1—Sheet rolling: the doubling process

not normally practicable, the sheets from this particular cast were marked 1 to 8 to preserve a record of their rolling position. Optical temperature readings taken during sheet rolling confirmed that the cast conformed to standard conditions in that the finishing-off temperatures were :

After third pass, prior to first doubling : 800–820° C.
After ' run-over ' pass : 880–900° C.
After final finishing-off pass : 830–850° C.

The next stage in the processing of sheets is cold-rolling, a very light treatment to produce flatness and a close surface texture, there being no measurable reduction in the thickness of the sheets. Sheets from this particular cast were cold-rolled in pairs with a single pass.

Annealing

This particular cast was annealed in a radiant-tube furnace, the sheets being assembled and placed at the centre of a load of transformer sheets (Fig. 2). The load was annealed at about 800° C., the temperature being recorded from thermocouples placed in the charge. Care was taken to ensure a slow rate of cooling and a flow of washed producer gas was directed into the cover. It is worth noting that while this gas probably promotes decarburization of the sheets, it may also give rise to undesirable oxide penetration. The annealing cycle is shown in Fig. 3.

Pickling, Washing, and Drying

When cold, the annealed sheets were pickled in two acid tanks, the first containing 10% sulphuric acid and the second 7% sulphuric acid. The sheets were then washed and dried on a continuous machine.

Selection of Sheets for Electrical Testing

When dry, all the sheets were sheared into Churcher test packs. Each sheet produced one test pack of 16 strips, each 30 × 4 in., eight strips being cut parallel to the sheet-rolling direction and eight at right-angles to this direction (Fig. 4). Portions of each sheet not required for the Churcher pack provided samples for microscopic examination.

Iron-Loss Tests

An iron-loss test at 50 cycles/sec. and 10,000 gauss was carried out on each Churcher pack, all values being recorded in watts per kilogramme. Details of the Churcher apparatus have been published elsewhere.[4] No other electrical tests were carried out.

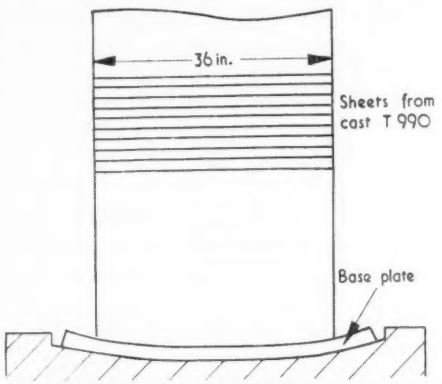

Fig. 2—Position of sheets from cast T990 in the centre of a 13-ton load during first annealing

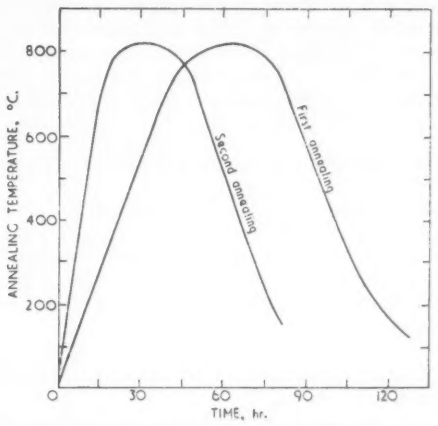

Fig. 3—Annealing cycles employed for sheets from cast T990. The marked difference is due to the two types of furnace used

Second Annealing

Under normal circumstances, Churcher packs have no further value after being used for the test. In this case, however, it was thought that interesting information might accrue from re-annealing and re-testing these packs. Throughout this paper, therefore, ' second annealing ' refers to the annealing of the packs. This process is, of course, essentially different in certain important respects from ' first annealing.' The main difference lies in the fact that, whereas in the first annealing the sheets formed part of the stack of full-sized sheets with a superficial area of 72 × 36 in., the second annealing was carried out on these small packs (area 30 × 4 in.) arranged in criss-cross fashion at the top of a furnace load (Fig. 5). The opportunity for reaction with gases was certainly much greater in the small Churcher packs at the second annealing than with the sheets of normal size at the first annealing. This is readily seen by comparing Fig. 5 with Fig. 2.

A further distinction between the two annealings is that, whilst the first was carried out in a radiant-tube furnace, a small batch-type furnace was used

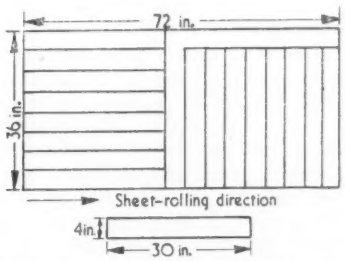

Fig. 4—Procedure for cutting Churcher strips: 16 strips, each 30 × 4 in. are cut from a 72 × 36-in. sheet to provide one Churcher pack; eight strips are cut parallel with, and eight at right-angles to, the rolling direction

for the second. Practical reasons prevented the use of the same furnace for both operations, as was, of course, to be desired. The second-annealing cycle is shown in Fig. 3. Temperature values for second annealing represent estimates derived from thermocouples near the furnace roof.

Further Treatment of the Double-Annealed Churcher Packs

Second annealing of the Churcher packs produced a light layer of scale which was removed in the laboratory by a short acid immersion. After this pickling, the packs were tested in the Churcher apparatus and were subsequently cut into specimens for micro-examination.

CHEMICAL ANALYSIS AND MICRO-EXAMINATION

Chemical Composition

During the teeming of ingot 4 a sample was taken and was later analysed (*see* below). The only chemical analyses on the sheet product were carried out on two samples, *A* and *B*, of first-annealed material. Specimen *A* came from a sheet which gave an iron-loss value of 0·94 W./kg., and *B* from a sheet giving a value of 1·40 W./kg. In view of the difficulties associated with the determination of small carbon contents, samples were analysed at two independent laboratories. Whereas sample *B* appeared to be slightly lower than *A* in carbon content, there was a significant discrepancy between the two results of the different laboratories in tests on the same specimen. In every case the barium hydroxide method[5] was used. Details of the analyses are given below:

Chemical Composition: Cast T990

Pit Sample

0·05% C, 4·20% Si, 0·11% Mn, 0·009% S, 0·017% P

Sheet Samples

	Sample *A*		Sample *B*	
	Lab. 1 %	Lab. 2 %	Lab. 1 %	Lab. 2 %
C	0·044	0·039	0·040	0·035
	0·045	0·038	0·042	0·035
	0·048	0·038	0·039	0·036
		0·039		0·037
Si	4·11	...	4·11	...
Mn	0·10	...	0·10	...
Ni	Trace	...	Trace	...
Cr	Trace	...	Trace	...
Cu	0·04	...	0·03	...
S	0·01	...	0·012	...
P	0·01	...	0·01	...

Micro-Examination

Small samples of sheet bar from each ingot and of annealed sheet were prepared for the microscope and were examined, first, in the polished state and, later, after etching with 10% nital. The features studied were the amount of non-metallic inclusions, carbide dispersion, grain shape, and grain size.

These examinations carried out at magnifications of up to × 500 proved of no value in accounting for iron-loss trends throughout the cast. In general, in fact, it was possible to observe in one small sample variations in carbide dispersion and grain size almost as great as those seen between all samples. The recorded observations are not considered of sufficient value to include in this paper, but there was a remarkable freedom from visible non-metallic inclusions throughout the whole cast.

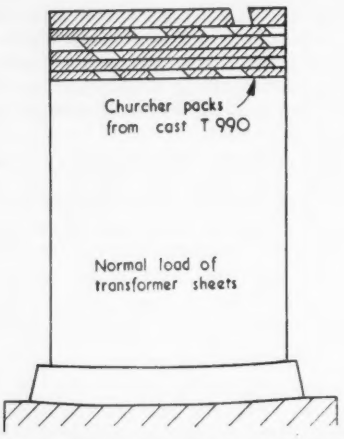

Fig. 5—Criss-cross arrangement of Churcher packs from cast *T*990 during second annealing. The packs are seen at the top of an 11-ton load

ANALYSIS OF IRON-LOSS VALUES FROM INGOT 2

VALUES AFTER FIRST ANNEALING

After the first annealing 160 sheets from ingot 2 were tested. Two sheet bars were taken from each of the 20 positions in this particular ingot, the positions being numbered 1 to 20, as shown in Fig. 6. The pair of sheet bars from each position was matched in the sheet-rolling process (as already explained) and produced eight finished sheets, numbered 1 to 8, according to the position in the rolling process (Fig. 1); this position is referred to as the position in the rolled pack.

The mean iron-loss values of 20 sheets from each position in the rolled pack are plotted in Fig. 7, and the mean values of the eight sheets from each position in the ingot are plotted in Fig. 8.

Analysis of Variance

An analysis of variance, shown in Table II, explains the total variation of the 160 iron-loss values as a sum of three components: (i) that due to difference

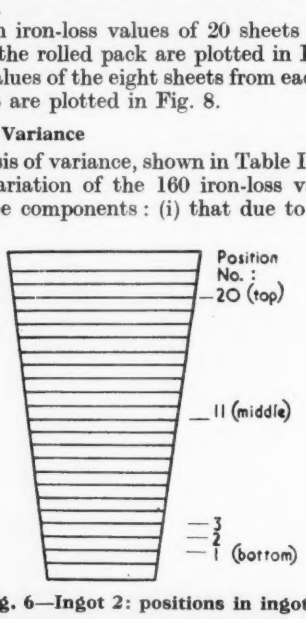

Fig. 6—Ingot 2: positions in ingot

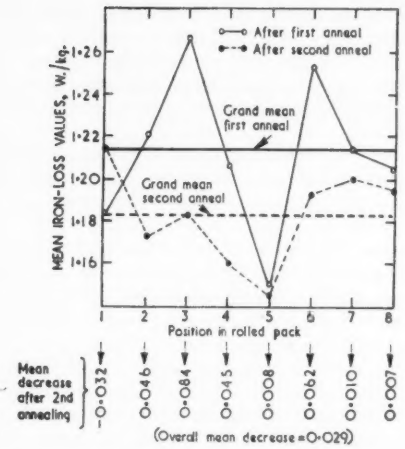

Fig. 7—Effect of position in rolled pack (ingot 2)

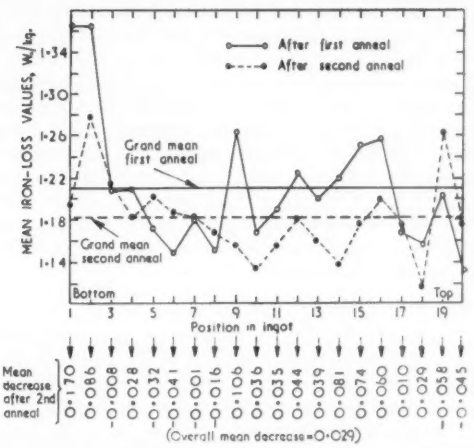

Fig. 8—Effect of position in ingot (data from ingot 2)

between positions in the rolled pack, (ii) that due to positions in the ingot, and (iii) a residual component which gives an estimate of the error variance of the observations when the effects of 'position in rolled pack' and 'position in ingot' have been eliminated. This analysis of variance shows that there are highly significant differences among the mean values of readings grouped according to position in rolled pack and also of readings grouped according to position in ingot. The residual variance of a single iron-loss reading when these two effects are eliminated is given in Table II as 0·00451, so that the standard error of a single reading is estimated at 0·067 W./kg. or about 5·6%.

Effect of Position in Rolled Pack

Examination of Fig. 7 giving the mean iron-loss values for each position in the rolled pack plotted against the position numbers, shows that positions 3 and 6, on the whole, have given relatively poor material, *i.e.*, the mean iron-loss is higher, whereas positions 1 and 5 have given a low mean iron-loss. The four other positions in the rolled pack have given values at, or about, the grand mean of all the 160 values obtained after the first annealing; possibly positions 4 and 8 tend to give better values than the overall average and positions 2 and 7 to give values about equal to or slightly worse than the overall average, but these tendencies are not particularly marked.

The highly significant effect of the position in the rolled pack noted in the above statistical analysis is largely due to the deviations of positions 3 and 6, on the one hand, and positions 1 and 5, on the other, from the overall average. The practical implications of this effect are discussed in greater detail in later sections.

Effect of Position in Ingot

Figure 8 does not indicate any uniform trend in the quality of sheets produced from different positions in the ingot, but it does show that the two bottom positions 1 and 2 produced exceptionally low-quality material. This may arise from the fact that this ingot was teemed at a high temperature—it was only the second to be teemed—and at a fairly rapid rate (the teeming time was 46 sec.), the cooling process resulting in an unusually high degree of heterogeneity. The hypothesis that an early ingot may be exceptionally heterogeneous could be tested by a study of further casts. Apart from the mean iron-loss of positions 1 and 2, the other values show considerable fluctuation about the grand mean, with a definite tendency for low values among positions at the top of the ingot (positions 17, 18, and 20).

VALUES AFTER SECOND ANNEALING

The mean values after the second annealing for the eight positions in the rolled pack are plotted in Fig. 7 and those for the 20 positions in the ingot in

Table II

ANALYSIS OF VARIANCE OF IRON-LOSS VALUES FROM INGOT 2 AFTER FIRST ANNEALING

Source of Variation	Degrees of Freedom	Sum of Squares	Mean Square	Variance Ratio (F) and Tests of Significance
Positions in rolled pack	7	0·1945	0·0278	$F = 6·16$ (probability less than 0·001; highly significant)
Positions in ingot	19	0·6259	0·0329	$F = 7·29$ (probability less than 0·001; highly significant)
Error	133	0·6000	0·00451	...
Total	159	1·4204	...	...

Fig. 8. The corresponding mean decreases in iron-loss are also shown ; these figures are the average actual improvements attained by the second annealing for different positions in the rolled pack and in the ingot.

An analysis of variance of the decreases similar to the analysis shown in Table II shows that there are highly significant differences among the mean decreases for different positions in the rolled pack and similarly for different positions in the ingot. Also, the overall decrease in iron-loss for all the 160 sheets is highly significant, being about five times its estimated standard error.

Effect of Position in Rolled Pack

The highly significant differences among the mean decreases for different positions in the rolled pack are amply illustrated in Fig. 7, from which it is seen that the positions producing the poorest material after the first annealing, positions 3 and 6, have shown the greatest average improvement after the second annealing, *viz.*, 0·084 and 0·062 W./kg., respectively.

The other positions, with the exception of position 1, all show some improvement ; in positions 2 and 4 it is quite marked ; in 5, 7, and 8 it is relatively small, but position 1, which gave high-quality material on the whole after the first annealing, actually shows a deterioration after the second annealing.

Effect of Position in Ingot

The highly significant differences among decreases for different positions in the ingot, Fig. 8, indicate that the improvement in quality is not distributed uniformly over all positions ; those giving the highest mean values after the first annealing have shown the greatest improvement, but those giving the best material after the first annealing have, on the whole, actually shown a deterioration after the second annealing.

ANALYSIS OF COVARIANCE OF DECREASES AFTER SECOND ANNEALING

The individual iron-loss values indicated a marked correlation between the iron-loss values obtained from a particular sheet after the first annealing and the subsequent decrease after the second annealing ; high iron-losses after the first annealing tended to be associated with substantial decreases in iron-loss after the second annealing and low values after the first annealing, with an actual increase of iron-loss after the second. Thus, the overall effect of the second annealing appears to be to improve low-quality material and to make the best material worse, as noted above.

We are thus faced with the problem of estimating the extent and effect of this correlation. The specific questions are : (1) What is the overall magnitude of this correlation and is it significant ? (2) If this correlation is taken into account, do the mean decreases after the second annealing for different positions in the rolled pack still show significant differences ; and does this also apply to the other factor, position in the ingot ? These questions are answered by means of the technique of the analysis of covariance. In applying this technique it is assumed that the decrease in iron-loss after the second annealing can be represented in terms of the value after the first annealing by a linear regression function.

The decreases after the second annealing are adjusted for the effect of the value after the first annealing by eliminating the part due to this linear regression function, and the adjusted decreases are analysed further to study the effect of position in the rolled pack and position in the ingot when the effect of the values after the first annealing has been accounted for by this linear regression. The appropriate analysis of covariance is shown in Table III.

In Table III, b_1 is the regression coefficient in the linear regression relation expressing the mean decreases after the second annealing for different positions in the rolled pack in terms of the mean iron-loss values after the first annealing for these positions ; similarly b_2 typifies the regression relation between the mean decreases after the second annealing for different positions in the ingot in terms of the mean iron-loss values after the first annealing for these positions ; and b_3 is the appropriate regression coefficient in the linear regression relation determined by the residual sum of products and the two residual sums of squares. The corresponding correlation coefficients are r_1, r_2, and r_3, respectively. These correlations all have high positive values, r_1 and r_2 being very nearly equal. This positive correlation has already been anticipated when noting above that

Table III

ANALYSIS OF COVARIANCE OF DECREASES IN IRON-LOSS VALUES FROM INGOT 2 AFTER SECOND ANNEALING AND THE VALUES AFTER FIRST ANNEALING

Source of Variation	Degrees of Freedom	Sum of Squares		Sum of Products	Regression Coefficients	Correlations	Sum of Squares due to Regressions	Residuals	Degrees of Freedom of Residuals
		Values after First Anneal	Decreases after Second Anneal						
Positions in rolled pack	7	0·1945	0·1903	0·1556	$b_1 = 0·8000$	$r_1 = 0·809$	0·1245	$R_1 = 0·0658$	6
Positions in ingot	19	0·6259	0·5016	0·4524	$b_2 = 0·7228$	$r_2 = 0·807$	0·3270	$R_2 = 0·1746$	18
Residual error	133	0·6000	0·6233	0·4250	$b_3 = 0·7083$	$r_3 = 0·695$	0·3010	$R_3 = 0·3223$	132
Total	159	1·4204	1·3152	1·0330					

the higher iron-loss values after the first annealing tend to be associated with the largest decreases after the second annealing and *vice versa*.

Applying the appropriate significance test of the regression coefficient b_3, the decreases after the second annealing are significantly accounted for in terms of the values after the first annealing by a linear regression relation and the regression coefficients b_1 and b_3 do not differ significantly.

Effect of Position in Rolled Pack

The mean decreases for different positions in the rolled pack still differ significantly when the effect of the value after the first annealing has been eliminated by means of a linear regression function. This means that the values after the first annealing do not explain the highly significant differences in the mean decreases of the eight positions in the rolled pack ; when these means are adjusted to a common value after the first annealing, they still differ significantly. These adjusted means are shown in Table IV.

It is now possible to discuss the effect of different positions in the rolled pack in rather more detail than hitherto. Figure 1 shows that sheets occupying positions 1, 4, 5, and 8 in the rolled pack all originate from one sheet bar (denoted *A* in Table IV), whilst sheets in positions 2, 3, 6, and 7 originate from the second sheet bar *B* ; this latter sheet bar occupied the inside position after doubling. Table IV shows that after the first annealing the average iron-loss of all sheets from sheet bar *A* is distinctly lower than that of all from bar *B*. The respective means are 1·185 and 1·238, the difference being highly significant. Thus, sheets from bar *A* occupying the outside position at the first bending give, on the whole, the better-quality material. This may be accounted for by the fact that in the heating process following the first doubling the outer bar suffers greater exposure to the furnace atmosphere and has, therefore, a greater opportunity for decarburization, which is known to favour low iron-loss values in transformer sheet.

Figure 1 indicates that sheets may be arranged in groups in descending order of exposure to furnace atmosphere as follows :

Group (i) : Positions 1 and 8 (most exposed)
Group (ii) : Positions 4 and 5
Group (iii) : Positions 2, 3, 6, and 7 (least exposed).

If a decarburization theory were sufficient to explain the observed differences in the mean iron-losses for different positions in the rolled pack, the iron-losses after the first annealing would be expected to accord with this order of exposure : *viz.*, group (i) would give the lowest mean iron-loss and group (iii) the highest. The actual means are : group (i), 1·192 ; group (ii), 1·178 ; group (iii), 1·238. Thus, whilst group (iii) gives, on the whole, the worst material, as was expected, the best material is given, not by group (i), but by group (ii). Now, it is known that in industrial heating furnaces it is difficult to achieve the desirable effect of decarburization without an accompanying tendency for oxide pick-up and, in some cases, also sulphide pick-up. Preece, Richardson, and Cobb[6] have discussed the penetration of scale in silicon steel under certain conditions of exposure. Following this line of argument it is not unreasonable to suppose that in group (i) the advantages accompanying decarburization have been, to some extent, nullified by the harmful penetration of oxides and/or sulphides, whereas the sheets in group (ii), which have been exposed to the furnace atmosphere for a shorter period, have received the benefits of decarburization without suffering excessive oxide penetration. Further, the sheets in group (iii) have experienced only a slight exposure to furnace atmosphere and may be considered to have been relatively unaffected by the heating operations ; they will thus have benefited little from the effect of decarburization.

In considering the effect of annealing it may be recalled that this is a vital process primarily designed to eliminate lattice strains, which affect electrical properties adversely. In addition to residual cold-work, strains are set up by impurities within the grain, notably carbon ; furthermore, interference between differently oriented grains gives rise to strain at grain boundaries. Annealing removes cold-work, causes migration of impurities to grain boundaries where they are less harmful, and gives rise to appreciable grain growth, a smaller number of grain boundaries and, therefore, less grain-boundary strain.

Annealing also affects the chemical composition of

Table IV

ADJUSTED MEAN DECREASES AFTER SECOND ANNEALING FOR DIFFERENT POSITIONS IN THE ROLLED PACK (INGOT 2)

Position in Rolled Pack	Mean Value after First Anneal	Rank in Terms of Quality*	Mean Decrease after Second Anneal	Rank in Terms of Decreases	Adjusted Mean Decreases† (2)	Rank in Terms of Adjusted Decrease	From Sheet Bar	Degree of Exposure to Furnace Atmosphere
1	1·180	2	−0·032	8	−0·009	8	A	(i)
2	1·220	6	0·046	3	0·040	4	B	(iii)
3	1·266	8	0·084	1	0·046	3	B	(iii)
4	1·206	4	0·045	4	0·049	2	A	(ii)
5	1·150	1	0·008	6	0·052	1	A	(ii)
6	1·256	7	0·062	2	0·031	5	B	(iii)
7	1·212	5	0·010	5	0·010	7	B	(iii)
8	1·204	3	0·007	7	0·013	6	A	(i)

* The lower the mean iron-loss the higher the quality.
† The residual regression coefficient b_2 has been used to derive these adjusted mean decreases.

sheet material and, although there is much to be learned concerning the reaction of gases on transformer sheet during annealing, it is known that decarburization does occur and is favourable to low iron-loss values. It is also known that over-long annealing periods and excessively high annealing temperatures often lead to unfavourable iron-loss values. In fact, it is usual to think of the annealing process in a similar way to the heating-for-rolling process, *i.e.*, to consider, on the one hand, the favourable effect of decarburization and, on the other, the unfavourable effects accompanying a too prolonged exposure to furnace atmosphere. There is some difficulty in defining these unfavourable effects, but they are believed to include oxide/sulphide penetration and loss of silicon at the sheet surface. If this were an entirely adequate account of the effect of the annealing process it would be possible to predict the overall effect of a second annealing process in the following way: Sheets in group (i) (positions 1 and 8 in the rolled pack) already decarburized to a considerable extent and possibly suffering the unfavourable effects of over-exposure may be expected to deteriorate after a second annealing; sheets in group (ii) (positions 4 and 5), which after the first annealing appear to have benefited from decarburization without suffering the effects of over-exposure, may be expected to remain relatively unaffected, improving slightly if they have not received the optimum exposure, or deteriorating slightly if they have been exposed more than the optimum amount; sheets in group (iii) (positions 2, 3, 6, and 7) not appreciably decarburized after the first annealing may be expected to undergo considerable improvement after a second annealing.

This is largely borne out by the figures in Table IV. Sheets in group (iii) show considerable improvement after the second annealing, the mean decrease for this group being 0·050 W./kg., sheets in group (ii) show some improvement, and sheets in group (i) actually show a mean increase of 0·012, indicating an overall deterioration in quality.

The above explanation of the effect of the annealing process leads us to expect the correlation between the iron-loss values after the first annealing and the decreases after the second annealing that have been noticed and taken account of in the analysis of covariance, but the mean decreases for different positions in the rolled pack are still significantly different when the effect of the correlation has been eliminated by a linear regression function. This means that differences among these mean decreases are not attributable merely to the values obtained after the first annealing. Further, Table IV shows that the adjusted mean decreases, when the effect of this correlation has been eliminated, are as follows: Group (i), 0·002 W./kg.; group (ii), 0·050 W./kg.; group (iii), 0·032 W./kg. These figures seem to indicate that, whatever values are obtained after the first annealing, as long as the sheets have not suffered already from over-exposure (as in the case of group (i)), a second annealing can do no harm and may do a considerable amount of good. In particular, sheets from positions 1 and 8 have not responded to a second annealing to anything like the extent shown by sheets from positions 4 and 5, whilst the other positions, particularly 3 and 6, all show an appreciable response.

This attempt to explain, in terms of decarburization and scale penetration, the pattern of core-loss values displayed in a pack of transformer sheets is not intended to represent an exhaustive study of the problem; it is merely the presentation of a reasonable hypothesis which can be tested by further work. Other explanations for the pattern may be more satisfactory. For instance, it has been suggested that the difference between the eight sheets in the pack can be accounted for in terms of strains induced in rolling.

Table V

ADJUSTED MEAN DECREASES AFTER SECOND ANNEALING FOR DIFFERENT POSITIONS IN THE INGOT (INGOT 2)

Position in Ingot	Mean Value after First Anneal	Rank in Terms of Quality	Mean Decrease after Second Anneal	Rank in Terms of Quality	Adjusted Mean Decrease	Rank in Terms of Adjusted Decrease
1 (Bottom)	1·365	20	0·170	1	0·062	5
2	1·364	19	0·086	3	−0·022	19
3	1·208	12	−0·008	14	−0·005	18
4	1·210	13	0·028	12	0·029	10
5	1·171	7	−0·032	17	−0·003	17
6	1·148	2½	−0·041	18	0·004	16
7	1·182	8	−0·001	13	0·020	14
8	1·152	4	−0·016	16	0·026	12
9	1·264	18	0·106	2	0·069	3
10	1·170	6	0·036	9	0·066	4
11	1·192	9	0·035	10	0·049	6
12	1·226	15	0·044	7	0·034	9
13	1·200	10	0·039	8	0·047	7
14	1·220	14	0·081	4	0·075	1
15	1·252	16	0·074	5	0·046	8
16	1·259	17	0·060	6	0·027	11
17	1·168	5	−0·010	15	0·021	13
18	1·148	2½	0·029	11	0·074	2
19	1·206	11	−0·058	20	−0·054	20
20	1·132	1	−0·045	19	0·012	15

Effect of Position in Ingots

From the analysis of covariance, Table III, the appropriate statistical significance test shows that the mean decreases for different positions in the ingots still differ significantly after adjustment for the effect of the values after the first annealing. The adjusted mean decreases are shown in Table V, the regression coefficient b_3 being used again to eliminate the effect of the values after the first annealing by means of a linear regression function.

Table V shows that some positions (*e.g.*, 14 and 18) give a much greater improvement after the second annealing than can be explained by a consideration of the corresponding values after the first annealing, whilst other positions (*e.g.*, 2 and 16) do not show anything like the sort of improvement to be expected. This differential response to second annealing for different positions in the ingot, illustrated by the fact that the values after the first annealing are not by themselves sufficient to explain the significant differences between the positions, is probably due to a very considerable heterogeneity in the composition of the ingot.

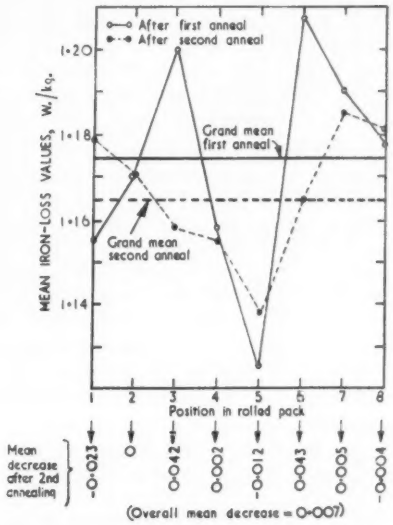

Fig. 9—Effect of position in rolled pack (data from whole cast)

ANALYSIS OF IRON-LOSS VALUES FROM THE WHOLE CAST

VALUES OBTAINED AFTER FIRST ANNEALING

Six sheet bars were taken from each of ingots 2 to 12, two from each of three selected positions in the ingot, *viz.*, bottom, middle, and top, corresponding to positions 1, 11, and 20, respectively, in Fig. 6. The pair of sheet bars from each position in each ingot was matched in the sheet-rolling process and produced eight finished sheets, numbered 1 to 8, according to the position in the rolling process (*see* Fig. 1). Thus, the data from the whole cast concerned 11 ingots, three positions in each ingot, and eight positions in the rolled pack, the study involving 264 sheets in all. These came from a total of 1760 sheets for the whole cast.

The main features of the iron-loss values obtained after the first annealing from the corresponding Churcher packs are shown in Figs. 9, 10, and 11.

The analysis of variance of these values after the first annealing is shown in Table VI. This analysis shows that there are highly significant differences among the mean values for the three positions in the ingot. This verifies what has already been discovered from the data of ingot 2. In addition, the analysis indicates highly significant differences among the eleven ingot means. Only one of the interaction terms in the analysis is found to be significant, *viz.*, the ingot × position-in-ingot interaction, which is highly significant. The residual

Table VI

ANALYSIS OF VARIANCE OF IRON-LOSS VALUES FROM THE WHOLE CAST AFTER FIRST ANNEALING

	Degrees of Freedom	Sum of Squares	Mean Square	Variance Ratio (F) and Tests of Significance
Source of variation:				
Ingots	10	0·3167	0·0317	$F = 10·5$ (probability $< 0·001$; highly significant)
Positions in rolled pack	7	0·1653	0·0233	$F = 7·72$ (probability $< 0·001$; highly significant)
Positions in ingot	2	0·0440	0·0220	$F = 7·28$ (probability $\sim 0·001$; highly significant)
Interactions :				
Ingots × positions-in-rolled-pack	70	0·2389	0·0034	$F = 1·13$ (probability $\sim 0·25$; not significant)
Positions-in-rolled-pack × positions-in-ingot	14	0·0551	0·0039	$F = 1·30$ (probability $\sim 0·25$; not significant)
Ingots × positions-in-ingot	20	0·3380	0·0169	$F = 5·60$ (probability $> 0·001$; highly significant)
Residual	140	0·4234	0·00302	
Total	263	1·5814		

variance of a single iron-loss value is given by 0·00302 W./kg., which is smaller than that obtained in the analysis of Table II on values after the first annealing from ingot 2; in the present case the estimate of the standard error of a single reading is 0·055 W./kg., or about 4·7%.

Effect of Position in Rolled Pack

The pattern in Fig. 9 is almost exactly the same as that for the data from ingot 2 (*see* Fig. 7). Positions 3 and 6 have given the worst material, and positions 1, 4, and 5 the best material, on the whole, the other three positions, 2, 7, and 8, giving intermediate mean iron-loss values. It is worth noting that in the present case the range of variation of the mean values for the different positions in the rolled pack is less than in the case of the data from ingot 2, *viz.*, 1·126–1·208 as against 1·150–1·266 for the former case.

Effect of Position in Ingot

The mean values for the three positions in the ingot (Fig. 11) are significantly different according to the analysis of variance of Table VI. This was to be expected from experience of the data from ingot 2. On the whole, the top position gives the best material and the bottom the worst. In six of the ingots, the top sheets are of the highest average quality, whilst this is true of bottom sheets in only two ingots and of middle sheets in only three ingots. In this way the results from the whole cast reinforce the tendency noted in the analysis of the results from ingot 2.

Differences Among Ingots

The mean iron-loss values for the 11 ingots considered in this investigation, shown plotted in Fig. 10, reveal significant differences according to the analysis of variance in Table VI. In discussing these differences, the main points which need to be considered are: (1) It is reasonable to expect that the earlier ingots will contain more non-metallic inclusions than the later ones, since these latter can be expected to benefit from an effectively longer ladle holding-time, allowing more time for the flotation of non-metallic inclusions; but this effect may be expected to diminish towards the end, since the falling temperature and the greater viscosity of the metal will tend to prevent further flotation of non-metallic materials.

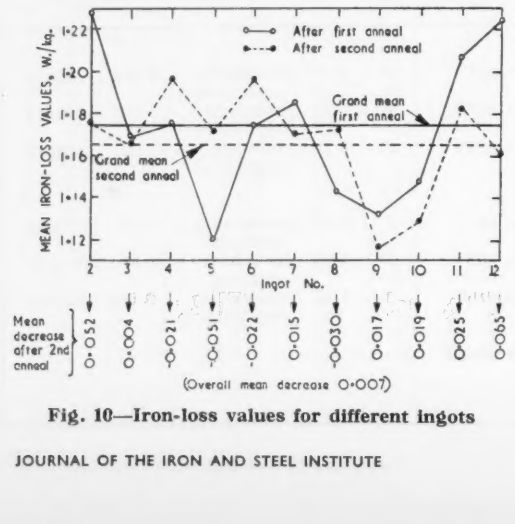

Fig. 10—Iron-loss values for different ingots

(2) The pouring rate, which is an indication of the combined effects of the viscosity and ferrostatic head of metal in the ladle, is affected by wear on the nozzle and by the use of the burner, which always increases nozzle size and pouring rate. This is clearly demonstrated in Table I: pouring rate decreased until ingot 5 had been teemed, the burner was then used, and this caused an immediate increase in pouring rate, after which the rate diminished steadily as pouring continued. The use of the burner can be expected, in general, to have a bad effect, since particles of refractory material tend to fall into the mould. Another consideration is that the later ingots are always liable to suffer from nozzle wear, which involves the picking up of non-metallics. (3) The accuracy of centring during pouring is likely to affect ingot surface and, in turn, the surface of sheet bars, but is, perhaps, unlikely to affect iron-losses.

All these considerations of differences between ingots amount to a discussion of the nature and causes of ingot heterogeneity. The first consideration, together with the effect of nozzle wear on the later ingots, would lead us to expect a **U**-shaped curve for Fig. 10, showing an improvement in quality among the early ingots, a fairly steady standard of quality among the middle ingots, and a gradual deterioration among the later ingots. Considerable dislocation from a general **U**-shaped curve has clearly taken place in the present instance following the use of the burner after the pouring of ingot 5. There was an immediate rise in iron-losses and, after a decrease in iron-losses among ingots 7, 8, and 9, a **U**-shaped curve was resumed.

This may, or may not, be a valid explanation of the significant differences in iron-loss values among the ingots of this particular cast. It is, however, clear that in the course of manufacture each cast will have its own individual history, occasioned by the relevant material conditions, and the differences among the ingots in the present study do not, therefore, necessarily indicate a general state of affairs.

Interaction Effect: Ingot × Position-in-Ingot

This highly significant interaction effect in the statistical analysis of Table VI indicates that, for this particular cast, at any rate, the iron-loss associated with a sheet from a particular ingot is very materially affected by the position in the ingot from which the

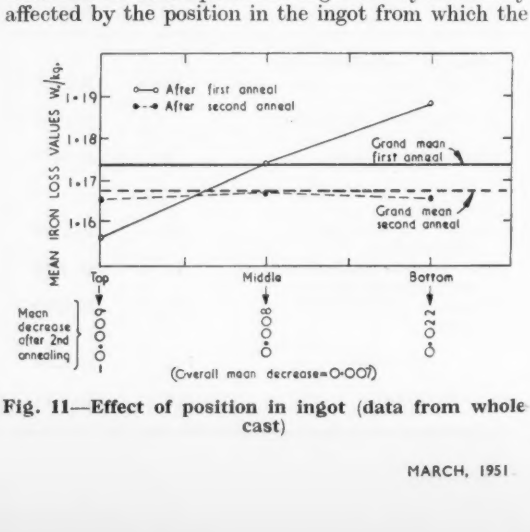

Fig. 11—Effect of position in ingot (data from whole cast)

sheet originated. It is, however, probably correct to infer that no general significance can be attached to this. The overall tendency shown in Fig. 11 is for high values to arise in the bottom position in the ingot, slightly lower values in the middle position, and lower values still in the top position. The significant interaction term in the analysis of variance is probably due to quite a small number of freakish values associated with certain ingots and certain positions in these ingots.

VALUES OBTAINED AFTER THE SECOND ANNEALING

Appropriate mean iron-losses and the corresponding mean decreases produced by this second annealing are plotted in Figs. 9, 10, and 11.

A very noticeable feature of the values after the second annealing is that their range of variation is distinctly smaller than that of the values after the first annealing. For example, the range of the mean values for the eight positions in the rolled pack is 1·137–1·186, as against 1·126–1·208 for the first annealing; for the ingots the range of variation is reduced from 1·120–1·228 to 1·115–1·197. A similar effect can also be noticed in the values from ingot 2 in Figs. 7 and 8, and it is clearly consistent with the correlation between the values after the first annealing and the corresponding decreases in values produced by the second annealing. The high values after the first annealing give rise to marked decreases after the second annealing and the low values to much smaller decreases or even to increases in the case of the lowest values.

An analysis of variance of the decreases after the second annealing shows that the overall average decrease is 0·0067 W./kg., which is not statistically significant.

Effect of Position in the Rolled Pack

As in ingot 2, there are highly significant differences among the mean decreases for different positions in the rolled pack and the great similarity between the two cases is seen by comparing Figs. 7 and 9. Again, position 1 gave a clear average deterioration in quality, and positions 3, 4, 6, and 7 show some improvement. Positions 5 and 8, however, show an average fall in quality after this second annealing although in each case it is quite small.

Effect of Position in Ingot

Again, this effect is significant and the mean decreases are in accordance with the general tendencies shown in the case of ingot 2. The mean value after the first annealing for the bottom position was fairly high and the corresponding mean decrease after the second annealing was quite marked. For the top position the low mean value after the first annealing gave rise to an actual increase in mean value after the second. The middle mean value after the first annealing was very close to the overall mean for the whole cast, and the corresponding mean decrease after the second annealing is nearly zero.

Differences between Ingots

There are highly significant differences among the mean decreases for different ingots which are already foreshadowed by the significant differences in the mean iron-loss values after the first annealing.

Table VII

ANALYSIS OF COVARIANCE OF DECREASES IN IRON-LOSS VALUES FROM THE WHOLE CAST AFTER SECOND ANNEALING AND THE VALUES AFTER FIRST ANNEALING

	Degree of Freedom	Sum of Squares		Sum of Products	Regression Coefficient	Correlations	Sum of Squares due to Regression	Residuals	Degree of Freedom of Residuals
		Values after First Anneal	Decreases after Second Anneal						
Source of variation:									
Ingots	10	0·3167	0·2976	0·2310	$b_1 = 0·7294$	$r_1 = 0·752$	0·1685	$R_1 = 0·1291$	9
Positions in rolled pack	7	0·1653	0·1317	0·1186	$b_2 = 0·7175$	$r_2 = 0·804$	0·0851	$R_2 = 0·0466$	6
Positions in ingots	2	0·0440	0·0422	0·0430	$b_3 = 0·9773$	$r_3 = 0·998$	0·0420	$R_3 = 0·0002$	1
Interactions:									
Ingots × positions-in-rolled-pack	70	0·2389	0·2493	0·1689	$b_4 = 0·7070$	$r_4 = 0·692$	0·1194	$R_4 = 0·1299$	69
Positions-in-rolled-pack × positions-in-ingot	14	0·0551	0·1709	0·0793	$b_5 = 1·4392$	$r_5 = 0·817$	0·1141	$R_5 = 0·0568$	13
Ingots × positions-in-ingot	20	0·3380	0·4628	0·3066	$b_6 = 0·9071$	$r_6 = 0·775$	0·2781	$R_6 = 0·1857$	19
Residual	140	0·4234	0·7294	0·3232	$b_7 = 0·7633$	$r_7 = 0·582$	0·2467	$R_7 = 0·4827$	139
Total	263	1·5814	2·0839	1·2706					

Interactions

Position-in-Rolled-Pack × *Position-in-Ingot* — This effect is significant. No very great importance can be attached to this.

Ingots × *Positions-in-Ingot*—This is significant as was expected from the fact that the corresponding term in the analysis of variance of the values after the first annealing was also highly significant.

ANALYSIS OF COVARIANCE OF DECREASES AFTER SECOND ANNEALING

The appropriate analysis of covariance of the decreases in iron-loss values from the whole cast after the second annealing, these decreases being represented in terms of the values after the first annealing by a linear regression function, is shown in Table VII. Again the correlations are quite large and positive.

The main conclusions from the statistical tests of significance are given below.

(1) The regression coefficient b_7 (= 0·7633) is indicative of a highly significant correlation between the values after the first annealing and the corresponding decreases after the second annealing, and is almost equal to the corresponding coefficient in the case of the data from ingot 2 (b_3 of Table III, which is 0·7083).

(2) The mean decreases for different positions in the rolled pack are accountable in terms of the values after the first annealing and do not differ significantly when the effect of these values is eliminated by a linear regression function.

The adjusted mean decreases for the different positions, the regression coefficient b_7 of Table VII being used to eliminate the effect of the values after the first annealing, are shown in Table VIII. Table VIII corresponds to Table V for the data from ingot 2. Again, after the first annealing, sheets in group (ii) were of the best quality, on the whole ; group (i) produced material of not such a high quality, whilst group (iii) produced the worst material. Again positions 3 and 6 were outstanding in this respect. The overall effect of the second annealing was to improve the material in group (iii) considerably, particularly sheets from positions 3 and 6, and to produce a slight deterioration in the material from group (ii) and a not inappreciable deterioration in group (i) material. The adjusted decreases show that group (i) material deteriorated more than might be expected from a consideration of the effect of the values after the first annealing in terms of a linear regression function, whilst group (ii) material did not deteriorate as might have been expected, and group (iii) material showed changes much as anticipated. These slight differences in response to the second annealing are, however, not sufficient to produce significant differences among the adjusted mean decreases.

The interpretation of the differences between different positions in the rolled pack in terms of exposure to furnace atmosphere and the consequent effects of decarburization and oxide/sulphide penetration is relevant to the features noted above. Positions 1 and 8 produce material that has already, after a first annealing, been adequately exposed to furnace atmosphere, so that the possible beneficial effects of further decarburization will be heavily outweighed by the harmful effects of oxide/sulphide penetration. The same is true, on the whole, of positions 4 and 5, although to a less extent. In very heterogeneous material, such as ingot 2, the material from these positions can possibly be improved by a second annealing process. On the other hand, sheets from positions 3 and 6 have not been adequately exposed to furnace atmosphere and can be very considerably improved by second annealing.

The greater similarities between Tables IV and VIII should be noted. It is reasonable to suggest that the differences are largely due to the fact that the material of ingot 2 in this particular cast exhibited a quite unusual degree of heterogeneity.

(3) There are no significant differences in the mean decreases after the second annealing for different positions in the ingots when the effect of the corresponding values after the first annealing has been eliminated. The adjusted mean decreases for the different positions are shown in Table IX. Any

Table VIII

ADJUSTED MEAN DECREASES AFTER SECOND ANNEALING FOR DIFFERENT POSITIONS IN THE ROLLED PACK (WHOLE CAST)

Position in Rolled Pack	Mean Value after First Anneal	Rank in Terms of Quality	Mean Decrease after Second Anneal	Rank in Terms of Decreases	Adjusted Mean Decreases	Rank in Terms of Adjusted Decreases	From Sheet Bar	Degree of Exposure to Furnace Atmosphere
1	1·156	2	−0·023	8	−0·010	8	A	(i)
2	1·172	4	0	5	0·001	5	B	(iii)
3	1·200	7	0·042	2	0·021	2	B	(iii)
4	1·158	3	0·002	4	0·013	4	A	(ii)
5	1·126	1	−0·012	7	0·024	1	A	(ii)
6	1·208	8	0·043	1	0·016	3	B	(iii)
7	1·191	6	0·005	3	−0·009	7	B	(iii)
8	1·177	5	−0·004	6	−0·007	6	A	(i)
Group (i)	1·166		−0·014		−0·009	(Positions 1 and 8)		
Group (ii)	1·142		−0·006		0·018	(Positions 4 and 5)		
Group (iii)	1·193		0·023		0·007	(Positions 2, 3, 6, and 7)		

Table IX

ADJUSTED MEAN DECREASES AFTER SECOND ANNEALING FOR DIFFERENT POSITIONS IN THE INGOT (WHOLE CAST)

Position in Ingot	Mean Value after First Anneal	Rank in Terms of Quality	Mean Decreases after Second Anneal	Rank in Terms of Decreases	Adjusted Mean Decreases	Rank in Terms of Adjusted Decreases
Bottom	1·188	3	0·022	1	0·011	1
Middle	1·175	2	0·008	2	0·006	2
Top	1·157	1	−0·009	3	0·003	3

differences of response to a second annealing are entirely accounted for by corresponding differences between the mean values after the first annealing. But, whilst this conclusion is perfectly valid for the three positions studied here for the whole cast, the data from ingot 2 showed that in an abnormally heterogeneous ingot there are very different responses to a second annealing in different parts of the ingot, which cannot be accounted for solely in terms of values obtained after the first annealing.

(4) After adjustment for the effect of the values after the first annealing, the mean decreases after the second annealing for different ingots still show significant differences (Table X), indicating very different responses to the annealing process by material from different ingots. This is further evidence of considerable differences in the composition of different ingots, 'composition' being interpreted in the broadest sense.

SUMMARY AND CONCLUSIONS

(1) This study of the variation in iron-losses in a single cast of silicon–iron transformer sheet has been concerned with (*a*) 264 sheets from 11 ingots of the cast, and (*b*) 160 sheets from a particular ingot, No. 2. The sources of variation considered are :

(i) Differences between ingots
(ii) Differences between different positions in an ingot
(iii) Differences in positions of the sheet in the sheet-rolling pack
(iv) Various interaction terms such as the differences between ingots for different positions in the ingots.

(2) For the 264 sheets from the 11 ingots of the cast the mean iron-loss after first annealing was 1·17 W./kg. at 50 cycles/sec. and 10,000 gauss ; the range was 0·89–1·48. In order of importance the significant factors contributing to variation were : differences between ingots, differences between positions in the sheet-rolling pack, and differences between positions in ingots.

Although the effect of a second annealing was to bring about an overall improvement in quality, the improvement was in the worst material, the best material being made worse by this second anneal. As a result, the range was reduced to 1·01–1·44 W./kg.

(3) For the 160 sheets from ingot 2 the mean iron-loss after first annealing was 1·21 W./kg. ; the range was 1·05–1·48. The two significant factors which contributed equally to the variation were : differences between positions in the ingots, and differences between positions in the sheet-rolling pack.

As in the case of the cast as a whole, a second annealing reduced the range, but the overall improvement was greater.

(4) It may be supposed that these variations in iron-loss are due to heterogeneity of structure and chemical composition, including variation in the dispersion of non-metallic and surface variation brought about by decarburization, desiliconization, and scale penetration. It is also well known that slight differences in lattice strain could account for variations in iron-loss values and these variations might arise in the sheet-rolling process, the annealing process, and even during the handling of finished annealed sheets.

All these variations must be due to lack of uniformity in production ; some of the processes are,

Table X

ADJUSTED MEAN DECREASES AFTER SECOND ANNEALING FOR DIFFERENT INGOTS (WHOLE CAST)

Ingot No.	Mean Value after First Anneal	Rank in Terms of Quality	Mean Decrease after Second Anneal	Rank in Terms of Decrease	Adjusted Mean Decreases	Rank in Terms of Adjusted Decreases
2	1·228	11	0·052	2	0·010	4
3	1·169	5	0·004	7	0·007	5
4	1·176	7	−0·021	8	−0·023	10
5	1·120	1	−0·051	11	−0·011	9
6	1·175	6	−0·022	9	−0·024	11
7	1·185	8	0·015	6	0·006	6
8	1·142	3	−0·030	10	−0·006	8
9	1·132	2	0·017	5	0·048	1
10	1·148	4	0·019	4	0·038	2
11	1·207	9	0·025	3	−0·001	7
12	1·225	10	0·065	1	0·025	3

by their nature, inevitably variable, whilst others are capable of being made more uniform by modified techniques.

(5) An important part of the total variation in iron-loss throughout the cast has been attributed to differences between ingots. This indicates that the various sources of ingot-to-ingot variability, *viz.*, teeming rate, effective ladle-holding time, and such irregularities as are brought about by the use of a burner to open out constricted ladle nozzles, are likely to be responsible for differences in ingot quality.

Variations in the teeming process are inevitable, but there is a clear need for planned experiments designed to minimize variability. Although, on the basis of this single cast, no clear-cut set of teeming conditions can be laid down, it is suggested that planned experiments should include (*a*) holding the ladle as long as possible before starting to teem, (*b*) teeming at as low a temperature as is compatible with conditions of fluidity, and (*c*) finding means of emptying the ladle very rapidly from the time teeming begins; uphill teeming in clusters or the use of a double stopper should be considered.

The best-quality sheets in this particular cast were produced from ingots 5, 8, 9, and 10. Ingots 2, 11, and 12 gave the worst average quality. Further casts should be studied with a view to determining whether or not a regular improvement from ingot to ingot can be expected with a falling off in the last two ingots or so.

(6) Variation in iron-losses arises within each ingot, the general tendency being for ingot bottoms to produce the worst-quality sheet and ingot tops the best. This effect is important and is quite marked, particularly in ingot 2, where the quality of the bottom was very much inferior to that of the top. The elimination of variability within ingots raises the same practical issues as those discussed in (5).

The fact that second annealing reduced within-ingot variability very considerably suggests that a good deal of ingot heterogeneity is of such a nature that it can be minimized by prolonged annealing in the sheet stage. For example, the distribution of carbon in the ingot may have been non-uniform and, by allowing sufficient opportunity for decarburization in the sheet stage, this lack of uniformity was largely removed.

(7) On the basis of the study of a single cast no conclusions can be drawn regarding the importance of variability in ingot-rolling temperatures. Other evidence indicates that, within limits, iron-losses are not affected by this variable.

(8) A striking source of iron-loss variation has been shown to exist in the sheet-rolling process. Variations between the eight sheets in a rolled pack follow a definite pattern, sheet 5 being markedly superior and sheets 3 and 6 decidedly the worst. From a close study of this pattern inferences have been made concerning decarburization and other surface effects which occur in the heating processes associated with sheet rolling. It appears that under particular conditions of furnace atmosphere there is an optimum time of exposure for giving best iron-losses, a certain exposure time being necessary to give adequate decarburization, whereas the harmful effects of scale penetration and desiliconization become dominant if a too long exposure is allowed.

Decarburization, desiliconization, and scaling effects merit detailed metallurgical study under the conditions of the operating furnaces. In particular, it is of interest to know whether the improved iron-losses reported on recent material heated in oil-fired furnaces can be explained in terms of the decarburization/scaling theory. Such a study might lead to establishing ideal heating times and temperatures and to further modifications in furnace atmosphere.

An immediate practical measure needing consideration is the separation of sheets according to their position in the rolled pack. Group (ii) sheets (from positions 4 and 5 in the rolled pack) might, if kept separate, be a source of high-grade material hitherto denied by pooling all eight sheets. Such a measure would be needed, in the first place, to confirm the findings on cast *T*990.

(9) Normally, variations in iron-loss in transformer sheets are, in part, attributable to differences in position in the stack of sheets during annealing. In the cast studied, however, all sheets were treated at approximately the same position in a load, and annealing variables have therefore been discounted. Interesting results were obtained by second annealing the Churcher packs. Whilst the overall improvement from an average iron-loss value of 1·1735 to a value of 1·1669 W./kg. was achieved by second annealing, which can be accounted only as of relatively slight importance, a much more substantial improvement was brought about in ingot 2. Considering that this second annealing was carried out on such small specimens and that they were placed at the top of a charge, the overall improvement brought about can be regarded as relatively small, and it might be inferred that first annealing had achieved, to a large extent, all that can be expected by ordinary industrial annealing. It is well known that hydrogen- and vacuum-annealing methods can effect improvements beyond the scope of ordinary furnace atmospheres.

A study of the improvements brought about by second annealing has helped towards an understanding of decarburization and scale penetration and has tended to confirm the theory suggested earlier in considering iron-loss variations between sheet positions in a rolled pack. Sheets already over-exposed in sheet rolling tended not to be improved by a second annealing whereas under-exposed sheets were improved appreciably. This was borne out by the high correlation between iron-loss values after first annealing and the corresponding decreases resulting from second annealing, the higher the initial iron-loss the greater the improvement.

It has also been shown for the cast as a whole that when a correction has been made for this correlation the decreases brought about by second annealing are not significantly different for different positions in the ingot or for different positions in the rolled pack. On the other hand, the corrected decreases for different ingots differ significantly. It would be unwise at this stage to interpret this in precise metallurgical terms, but it is an effect which may prove interesting in the light of further investigation.

Ingot 2, which, after the first anneal, was very inferior, responded much more readily to the second annealing than did the cast as a whole. A further abnormality of this ingot was brought to light by the analysis of covariance, which showed that, even after allowing for the correlation of initial values with decreases brought about by second annealing, the decreases were still significantly different for different parts of the ingot and for different positions in the rolled pack. In future studies it would be of interest to note whether or not, as a general rule, early ingots are subject to excessive heterogeneity and a consequent wide range in iron-loss values, and whether the general results of a second annealing in the special circumstances of this particular cast are found also in the case of second annealing of full-size sheets.

(10) The chemical analyses reveal that there is room for progress in arriving at a reliable technique for determining low carbon contents. It is striking that two samples having approximately the same carbon content were associated with such widely different iron-loss values as 0·94 and 1·40 W./kg. Although carbon is known to exert a profound influence on iron-losses other very important influences are evidently at work.

(11) Microscope studies of grain size and inclusion contents were valueless in explaining variability of iron-losses. The reasons for failure were probably the submicroscopic nature of deleterious non-metallic dispersions and the wide variability of grain size and inclusion count within a small metallographic specimen. This variability was great enough to vitiate comparisons between specimens from different parts of the cast. Whilst there is no reason to doubt the established fact that, within limits, a large grain size promotes low iron-losses, future studies might give greater attention to microscopic examination of the surface conditions on silicon–iron sheets.

(12) This study has revealed the complexity of the transformer-sheet problem and indicates that it cannot be completely solved until systematically planned experiments, designed to isolate the causes of variation at each stage of processing, are undertaken.

Acknowledgments

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Sinter Making at Appleby-Frodingham

By G. D. Elliot and N. D. Macdonald

SYNOPSIS

A large-scale plant research has been carried out with the aim of improving the quality of sinter manufactured from Frodingham and Northampton ironstone fines. Several aspects of plant, raw materials, and practice are briefly dealt with ; the effects of return fines, which are shown to be of great importance in sinter making, are discussed at greater length. Arising from this work, modifications are being made to a new sintering plant that is being built. These are mentioned but are not described.

Introduction

The first installation to sinter British ironstone was a Greenawalt plant at the North Ironworks of the Appleby-Frodingham Steel Company in 1934. Results from this plant were sufficiently encouraging to lead to doubling of capacity to 8000 tons per week in 1936. A Dwight Lloyd type of plant with a capacity of 12,000 tons per week was erected at the new South Ironworks of the same Company in 1939.[1] The addition of a plant with a further 12,000 tons per week sintering capacity is now in hand. Before this was designed, much full-scale plant research was carried out to improve the many unsatisfactory features associated with the existing sintering practice and equipment at the South Works.

At the outset of this work, in June 1947, it was decided that only full-scale operational research could give the information that was needed, and only in the blast-furnace could a check be made on whether the final product was good or bad. Laboratory work was not ignored but was used extensively only to further the plant work.

It is practically impossible to discuss a commercial sintering process as a whole—there are perhaps even more variables in sinter making than in ironmaking. The plant experiments were carried out over a period of two and a half years and are much too extensive

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Mr. Elliot and Mr. Macdonald are on the staff of the Appleby-Frodingham Steel Company, Scunthorpe.

to describe in this paper, but different aspects of sinter making are discussed in the light of the work carried out and mention is made of resulting plant modifications. Many unorthodox and novel features are being incorporated in the sinter plants now being built at Appleby-Frodingham, as a direct result of this work.

In addition to sinter-plant problems as such, many investigations had to be made to solve other problems as they arose. These included work on screening, dust extraction, fine crushing, instrumentation, sampling techniques, ore drying, and so on. The work included designing, and testing new schemes of various items of plant such as feeder tables, chutes, etc. It is believed that the plant now being built will be better equipped to deal with the peculiarities of sintering carbonate ores than was at one time believed possible.

Successful sintering practice using British carbonate ores is a problem with difficulties and peculiarities associated only with such a practice. The work described is not applicable to the sintering of rich ores with a low lime content or with a low loss on ignition. All the practice details recommended for Appleby-Frodingham are not desirable nor necessary for other practices. The peculiarities of the local ironmaking practice, based on the smelting of a self-fluxing mixture of Frodingham limey and Northampton siliceous ironstone, make special treatment necessary.

SINTER QUALITY

The quality of sinter, like that of coke, is difficult to assess. In both cases sampling presents difficulties and in neither material can quality be determined from a single test. Nor is a standard available by which sinter quality may be measured. As with coke, the only real test of sinter quality is behaviour in the blast-furnace.

Sinter quality can therefore be considered only in relation to the raw materials (including coke) and practice at the furnaces in which the sinter is to be smelted. There is considerable difference between good Appleby-Frodingham sinter and good Swedish sinter. Sinter of the latter quality will never be possible when the Fe content of the sinter is less than 40%.

Various workers have suggested the following tests of sinter quality :

Chemical	*Physical*
Content of ferrous oxide[2]	Minimum size
,, ,, lime[3]	Maximum size[6]
,, ,, fayalite[4]	Size range[6]
,, ,, unsintered material[5, 6]	Porosity[9]
,, ,, sulphur[7]	Reducibility[10, 11]
Degree of oxidation[8]	Strength measured by shatter test[12]

Unfortunately, since chemical properties are dependent on the raw materials available and as physical properties are often associated with analysis, the above standards are only possible with very pure raw materials. Given such raw materials, plant and practice can vary within very wide limits and still produce good sinter.

A satisfactory sinter for Appleby-Frodingham blast-furnaces can now be expressed in test figures, but before attempting to do so, it may be worth while recalling the commercial and technical aims of sintering at this plant. Briefly, they are to remove ironstone fines from the furnace burden and return them in better metallurgical condition. If $-\frac{1}{2}$-in. ore fines are removed, ideally $+\frac{1}{2}$-in. sinter should be returned. It is suggested that this is the primary aim of sintering, and the attainment of other desirable properties must be secondary to it.

Although sinter is a material different from ore fines, when the major part of the blast-furnace burden

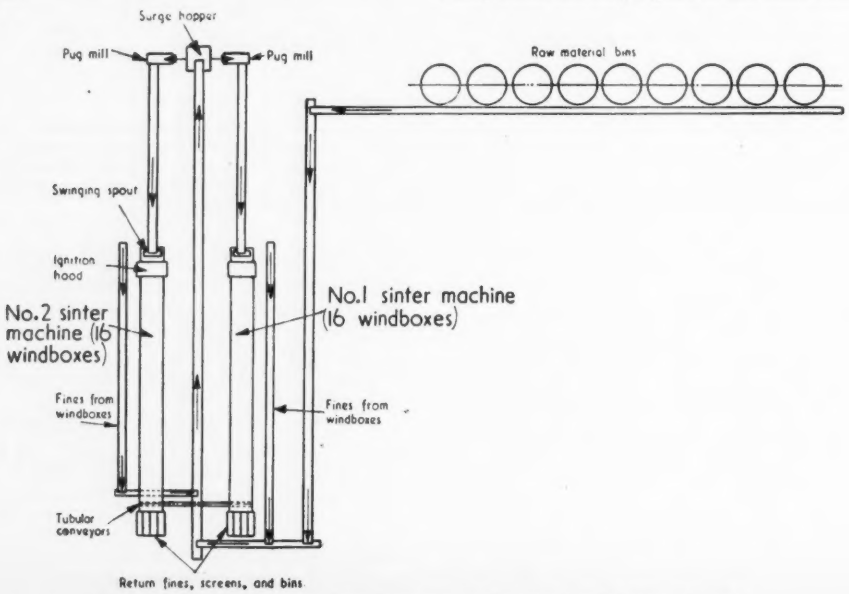


Fig. 1—Diagrammatic arrangement of Dwight Lloyd sinter plant

is lump ore, fines are undesirable, whether they be ore fines or sinter fines.

Normal sintering practice gives a product containing approx. 50% of + ½-in. material. Full-scale experimental work yielded a product containing 84% of + ½-in. material. In both cases, the screened-out ore fines from which the sinters were made contained about 10% of + ½-in. material. To achieve such an improvement, all other properties had to be ignored and only after this improvement had been obtained, could such factors as reducibility, oxidation, fayalite, etc., be considered as bearing on plant design or practice.

EXISTING SOUTH SINTERING PLANT

The plant consists of two A.O.R. Dwight Lloyd machines with a total nominal capacity of 12,000 tons/week. Each machine is 6 ft. wide and 108 ft. long, the total grate area being 648 sq. ft./machine. Machine speed can be varied between 35 and 90 in./min. Normal depth of sinter mixture on the grate is 12 in. and suction is usually between 12 and 15 in. W.G. Each machine is served by a 1000-h.p. suction fan capable of 150,000 cu. ft. of air per min. with a static suction of 21 in. W.G.

Raw materials consist of — ⅝-in. raw Frodingham fines, — ⅝-in. raw and semi-dried Northampton fines, flue dust and coke breeze crushed to — ⅛ in. in rod mills, which are fed to the gathering belt by a rotary feeder table. Return fines, handled by vibrating tubular conveyors, are fed on top of the raw material mixture on the gathering belt.

The division to two machines is effected by a feeder table with two gates below a surge hopper into which the gathering belt discharges. From this hopper each feed passes through a drum-type pug mill in which the necessary water is added, and then through a swinging spout to the machine. Ignition is effected by blast-furnace gas. The sinter discharges from the end of the machine over a static bar grizzly screen to wagons or to the storage pit. Here water spraying is necessary to reduce the temperature of the product. Figure 1 shows a diagrammatic arrangement of the existing plant.

SOME ASPECTS OF SINTER MAKING

Ignition

Although the importance of a fierce penetrating ignition has been stressed it is now believed that it is only necessary to ignite the coke particles in the surface layers of the mixture. As coke has an ignition temperature of only about 450° C., this is not usually a difficult matter. A very fierce penetrating ignition

Table I

EFFECTS OF VARYING MOISTURE CONTENT

No.	Suction at Fan Inlet, in. W.G.	Moisture, %	Ignition	Appearance of Bed after Passing the Ignition Surface	Remarks
1	17½	9·0	Dusty and rapid	Smooth carpet top. Blue black colour. No ripples	Good material segregation. Violent slagging. Top approx. 2 in. sintered
2	17	10·0	Dusty and rapid	Smooth carpet top. Blue black colour. No ripples	As No. 1
3	16	11·5	Normal	Normal	Good material segregation. Slagging, with greater depth of sintering than in 1 and 2, especially at the corners of the pallet
4	14	12·5	Normal	Normal	Sintered through. Satisfactory product
5	13½	15·0	Normal	Normal. Slight spitting after ignition	As 4 with better balling of material
6	14	16·0	Slightly less fierce	Slight puffiness. Brown colour	Porous sinter. Very good balling of raw materials
7	13½	17·0	Retarded	Increased puffiness. Brown colour	Sinter more porous than 6
8	12	18·5	More retarded than 7	Very puffy. Brown colour	Top 1½ in. calcined. Large patches of unsintered material. Sinter very open
9	13	21·0	No ignition possible	Very puffy. Brown colour	Sludge. No segregation of materials, merely sliding away from the cut-off plate. Top 1 in. dried out, remainder contained 16% moisture

may in fact cause the production of much dust in the top few inches of the bed because the carbon, under the effects of flame pressure from the ignition furnace and suction from the main fan, may be burnt out before sintering has time to take place.*

Provided that the more important details associated with size-grading of the raw materials are correct, ignition, broadly speaking, will look after itself. The behaviour of the material under the ignition hood is a useful guide to the quality likely to be produced, and often enables the operator to make adjustments in his practice.

As a result of the work done, it was decided to repeat the present design of ignition furnace in the new plant installation.

Moisture Content

If other more important factors are controlled the permissible range of moisture is greater than was formerly supposed. Many tests have shown that moisture in the sintering mixture, for normal raw materials, may vary between 12 and 17% with little effect on quality (*see* Table I). With $-\frac{1}{4}$-in. materials the permissible range of moisture is somewhat narrower because of the tendency of the hammer-milled material to cake at moistures in excess of 15%. The use of a rapid moisture tester of the calcium carbide type has been found extremely useful in experiments on the effects of moisture.

Much more important than the permissible range of moisture is the provision of rapid adjustment of the water to the mixer to meet variations below the swinging spout. The greatest cause of moisture variation lies in return-fines variations.

As a result of the tests carried out, the use of remotely controlled, electrically operated valves on the water lines to the mixers was investigated. These proved highly successful and valves of this type have been installed on the existing plant and are also being fitted on the new plant. Each valve has an indicator to show the setting of the valve. Provision is also being made to fit vibrators below the swinging spout in case caking of raw material becomes a problem.

Raw Material Size Segregation

Experimental work showed that segregation of ore sizes was in many cases more important and had greater effects than segregation of coke. Earlier theories that patchy sinter was caused by segregation of coke had to be discarded.

The problem of size segregation was further aggravated by feeding two machines from a single conveyor belt. Transfer of raw material from one conveyor to another caused segregation, the larger sizes rolling to the side of the receiving belt farthest from the point of delivery. Unless segregation was prevented, splitting the feed caused one machine to receive a rougher charge than the other. As a result of many tests, a simple revolving distributor was fitted in the throat of the surge hopper, as shown in Fig. 2, and this completely eliminated the trouble.

Other causes of segregation more difficult to overcome are :

(*a*) Initial size grading of raw materials
(*b*) „ „ „ „ return fines
(*c*) Design of swinging spout, cut-off plate, and similar items of plant.

The size range of raw materials can be narrowed only

* The best sinter, measured by any test known to the authors, was produced in a very old plant where ignition was effected by setting fire to wood shavings sprinkled over the surface of the mixture in the sintering pan. The actual ignition furnace was a shovelful of burning charcoal!

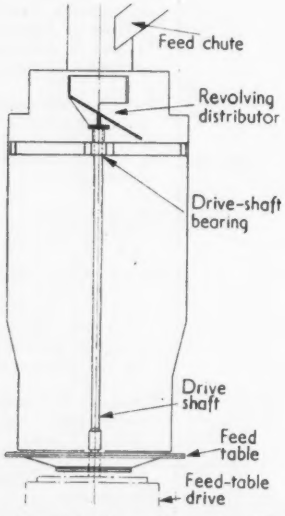


Fig. 2—Arrangement of revolving distributor in surge hopper

Table II

VARIABILITY OF DISCHARGE FROM FEEDER TABLES

Three series of 50 weighings were taken at each of the five feeder tables in use. Each weighing was collected in a scoop of approx. 30 lb. capacity ; the time to fill each scoop (750 weighings in all) was the same, being controlled by resting the scoop on the conveyor belt as it collected the total discharge from the table.

Material	Series	Standard Deviation		Coefficient of Variation
		Total	Intrinsic	
Frodingham, $-\frac{5}{8}$ in.	1	1·87	1·83	9·5
	2	0·98	0·95	6·6
	3	2·02	1·60	5·8
Frodingham, $-\frac{1}{4}$ in.	1	0·66	0·62	3·2
	2	0·52	0·54	3·9
	3	1·02	0·81	3·6
Dried Northants, $-\frac{3}{8}$ in.	1	0·44	0·40	5·0
	2	0·20	0·20	4·2
	3	0·58	0·57	5·6
*Flue Dust	1	0·78	0·53	18·7
	2	0·83	0·59	19·3
	3	1·56	0·95	15·6
Coke Breeze, $-\frac{1}{8}$ in.	1	0·101	0·093	4·2
	2	0·062	0·059	4·4
	3	0·102	0·104	3·9

* The high coefficient of variation was due to varying moisture content

by reducing the screen size at which fines are discarded after the run-of-mine ore has been crushed, or by crushing the fines so discarded. For reasons mentioned later, the latter course was adopted. Similar arguments apply to return fines—a subject often of overriding importance in sinter making (*see* p. 269).

No hard and fast rules can be laid down for the best design of feed arrangements to the machine. The swinging spout is considered to be the most efficient method of handling a close-sized grading material. As will be shown later, this proviso should apply for many more important reasons. The details of spout, cut-off plate, height of feed, etc., must be worked out for the practice and materials under consideration. Generally speaking, the discharge from the spout should impact against a vertical, or nearly vertical fixed surface, to provide maximum opportunity for the larger particles to segregate down the slope of the material, *i.e.*, the angle of rest. The height of the pile so deposited should be higher than the bottom of the cut-off plate (to prevent segregation in two horizontal directions and an irregular bed) and should be maintained at a very constant level to maintain a constant segregation and consequently a constant bed permeability. The latter is necessary to avoid the sintering air 'hunting' from one windbox to another, and so causing patchy sintering.

Feeder-Table Design

A serious cause of variations in product was traced to variations in feed from the feeder tables under the raw-material bunkers. Statistical analysis was carried out which directed considerable attention to the subject (*see* Table II).

Although improvement was obtained by minor adjustments to feeder-table design, a narrower size range of the material handled led to less variation, and gave greater improvement than any modification to design and construction of the feeder-table gates, etc. It is suggested, however, that feeder-table and raw-material bin design are subjects worthy of close investigation; it may well be that the final answer will not lie in a feeder table. It was decided that attention to size grading was of more pressing importance and would yield more immediate results.

The bunker filled from a belt tripper was found to give a more regular feed from the feeder table than a bin filled by grab crane. It appeared that loose filling was preferable. As a result, a new design of bin bottom was adopted for the new installation.

Mixing

The orthodox pug mill mixer installations were quite incapable of providing satisfactory mixing, because with the machines under discussion, each operating at a normal rate of 100 tons/hr. of raw material (including return fines), theoretical time in the mixer is only 36 sec. This time is insufficient when considered in relation to segregation, and variations in water content and in carbon content.

Size, temperature, and amount of return fines had a significant effect on the efficiency of mixing. Table III indicates the difficult problems often presented to the mixer.

As a result of this work, it was decided that future installations should be provided with much more generous mixing capacity, and that ample provision should be made for maintenance of mixers.

Screening of Sinter

Usually, the only screening of sinter is carried out as the product falls from the end of the machine into the discharge chute, and the screening is usually done over a fixed-bar grizzly. Each pallet containing about 900 lb. of sinter is discharged separately and only 2 sec. elapse between the first sinter of the pallet striking the screen and the last sinter of the pallet

Table III

PROPORTIONS OF SINTERED TO UNSINTERED MATERIAL IN FEED TO MIXER

(Samples from Surge Bin No. 1 Gate)

Time Samples Taken, p.m.	− 1 in. to + ¾ in.		− ¾ in. to + ½ in.		− ½ in. to + ¼ in.		− ¼ in. to + ⅛ in.		− ⅛ in. to + $\frac{1}{16}$ in.		− $\frac{1}{16}$ in.	
	Sintered, %	Unsintered, %	Sintered, %	Unsintered, %	Sintered, %	Unsintered, %	Sintered, %	Unsintered, %	Sintered, %	Unsintered, %	Sintered, %	Unsintered, %
5.15	36·5	63·5	37·0	63·0	25·2	74·8	11·6	88·4	8·5	91·5	15·7	84·3
5.45	10·3	89·7	23·5	76·5	20·0	80·0	10·5	89·5	8·7	91·3	12·8	87·2
6.15	21·7	78·3	22·0	78·0	20·0	80·0	11·0	89·0	10·0	90·0	17·8	82·2
6.45	39·4	60·6	31·9	68·1	22·2	77·8	11·7	88·3	10·5	89·5	10·0	90·0
7.15	26·3	73·7	33·9	66·1	30·0	70·0	12·0	88·0	7·0	93·0	26·0	74·0
7.45	37·1	72·9	43·4	56·6	25·0	75·0	16·2	83·8	8·0	92·0	17·5	82·5
8.15	17·0	83·0	23·0	77·0	29·5	70·5	14·5	85·5	9·0	91·0	21·3	78·7
8.45	11·0	89·0	30·6	69·4	29·5	70·5	14·0	86·0	12·6	87·6	14·3	85·7
9.15	26·5	73·5	38·1	61·9	15·5	84·5	16·0	84·0	14·5	85·5	42·0	58·0
9.45	17·6	82·4	32·3	67·7	36·3	63·7	17·9	82·1	14·0	86·0	15·5	84·5
10.15	31·6	68·4	49·4	50·6	36·0	64·0	17·0	83·0	13·6	86·4	20·4	79·6
10.45	0·6	99·4	54·8	45·2	44·0	56·0	23·0	77·0	15·5	84·5	24·7	75·3
Average	22·5	77·5	35·0	65·0	27·7	72·3	14·6	85·4	11·0	89·0	19·8	80·2

* The separation between the sintered and unsintered material was effected by hand picking the larger pieces and then carrying out a magnetic separation. The non-magnetic fraction is considered to be unsintered material

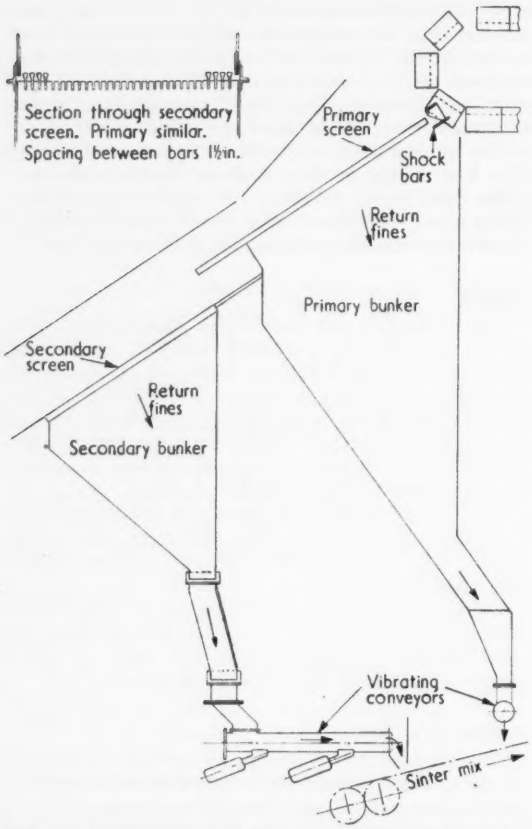


Fig. 3—Arrangement of screens in discharge chutes

leaving the screen. After the sinter cake strikes the shock bars above the screen, much of the smaller material becomes 'air-borne' and goes down the discharge chute without touching the screen. This inefficient screening is a major cause of so much dead fine material going forward in the oversize sinter and so to the blast-furnace. To improve screening efficiency, baffles, cascades, altered angles, deflectors, and wider bar spacings were all tried, and finally, a permanent modification to the plant was made by the addition of a second grizzly screen in the discharge chute below the original screen, virtually doubling the screen area (Fig. 3). Moving screens were not used because of obvious difficulties when screening hot sinter.

The efficiency of fixed-bar screens is dependent on sinter quality; the poorer and softer the sinter, the lower is the screening efficiency, and the greater is the amount of fine material passing with the sinter to the furnaces instead of to return fines.

In the new plant design final screen arrangements have been improved by the addition of a robust moving screen which will deal with the sinter after cooling, but before leaving the sintering plant.

Suction

The suction obtained at any one time during normal operation is controlled entirely by the material being fed to the sintering grate; variations in suction are an indication of variations in the material on the sintering machine. Given constant raw materials and homogeneous mixing, there should be little or no variation in suction. No combination of settings of the individual windbox valves was found to affect suction and it was decided to abandon the existing individual windbox valves and not to fit them in the new installation.

Final Cooling

If the last few feet of the machine are utilized to cool the product the machine is obviously not being used to its full sintering capacity. The use of a 1000-h.p. fan for cooling is expensive and, in addition, normal variations in practice make it impossible to end the sintering process always at the same point along the machine. If sinter is to be turned off the machine cool enough to prevent the paint on the receiving wagons being burnt, the plant will be reduced to about 50% capacity. Water quenching is therefore always necessary, but this is shown to cause breakdown and dust production (*see* Table IV).

Since the slag-forming minerals in home-ore sinter are partly present as glass, the reason for degradation by water quenching is obvious (*see* Fig. 4). The presence of free CaO from the fossil shells in Frodingham ironstone serves only to aggravate the problem (*see* Fig. 5).

The investigation into the cooling of sinter involved investigation into methods of handling, and, because of glassy constituents, it was found that there was less breakage when handling hot sinter than when handling cold sinter. The smaller the amount of breakdown in handling, the lower will be the acceptable quality of product at the end of the machine (*see* Table V).

As a result of this part of the investigation, it was decided to fit rotary after-coolers in the new plant installation and to provide belt conveyors to deliver sinter to the blast-furnace high-line in preference to the present method of delivery from the grab of the ore bridge.

Optimum Raw Material Size for Sintering

Most of the investigations so far described have shown the importance of the raw materials used. The three main raw materials are :

(*a*) Frodingham ironstone fines (with natural moisture)
(*b*) Northampton „ „ (with natural moisture)
(*c*) „ „ „ (semi-dried to 10% H_2O)

Their principal characteristics and their influence on sintering, are as follows :

Frodingham Ironstone Fines—After screening out at a nominal $\frac{5}{8}$ in., a typical size grading is as follows :

+¾ in.	+½ in.	+¼ in.	+⅛ in.	+⅛ in.	+16 m.
3·7	17·0	41·7	56·9	66·6	68·7

+30 m.	+60 m.	+100 m.	−100 m.
75·9	87·6	94·0	6·0

The larger pieces are too big to be sintered, especially when chemical analysis is considered. A typical as-received analysis is as follows :

H_2O	FeO	Fe_2O_3	CaO	SiO_2	Al_2O_3
12·8	7·41	24·62	16·9	9·2	5·21

Sintering

MgO	S	MnO	Loss	Total Fe
1·45	0·320	1·00	33·8	23·0

Although the figure for sintering loss is not strictly correct, because changes in oxidation of the iron have been ignored, this figure (moisture + CO_2 + combined water) is useful when considering the sintering properties of an ore.

A piece of this material, unsintered but calcined, is a prolific cause of dusting if it passes over the normally inefficient final screen, even if it is enclosed by fully sintered material. The porosity of sinter is usually high enough to permit water to act on the

Table IV

COMPARISON OF COOLING METHODS FOR SINTER

Alternate pallets on discharge from the strand were directed into separate wagons. This was continued until both the wagons were full. The sinter going into one wagon was subjected to a continuous stream of water—' water cooled.' The contents of the other wagon were allowed to cool without artificial means—' air cooled.'

Test	+4 in.	+2 in.	+1½ in.	+1 in.	+¾ in.	+½ in.	+¼ in.	+⅛ in.	+1/16 in.	−1/16 in.
Screen Tests (Air Cooled)										
1	...	...	6·1	14·1	32·4	63·8	89·9	94·4	97·2	2·8
2	...	...	12·1	21·4	37·6	67·2	92·4	95·0	97·6	2·4
3	...	...	8·0	20·4	41·0	71·6	86·2	97·0	98·9	1·1
4	...	1·4	4·3	13·0	30·8	61·9	93·0	95·0	97·4	2·6
5	10·9	26·6	34·6	45·6	57·2	70·5	93·0	95·2	98·6	1·4
6	17·0	40·2	51·8	63·7	72·3	82·8	96·2	97·9	99·3	0·7
Screen Tests (Water Cooled)										
1	...	...	2·4	7·9	17·9	50·9	87·7	92·7	97·3	2·7
2	...	...	6·4	14·4	32·5	63·1	89·2	94·4	98·9	1·1
3	...	...	1·7	6·5	25·3	57·6	90·9	95·6	98·6	1·3
4	...	0·5	0·9	2·2	7·6	28·4	77·9	84·2	95·5	4·5
5	...	1·0	3·2	8·8	18·8	36·9	83·0	88·3	98·2	1·8
6	1·2	15·9	29·3	43·8	55·0	69·9	91·3	95·2	99·3	0·7

Test	+1 in.	+¾ in.	+½ in.	+¼ in.	+⅛ in.	+1/16 in.	−1/16 in.
Shatter Tests* on 1-in. Material (Air Cooled)							
1	31·4	54·7	74·9	87·6	95·1	97·4	2·6
2	44·2	61·6	78·8	93·8	95·6	97·8	2·2
3	25·7	53·0	75·8	93·2	95·4	97·8	2·2
4	18·9	40·7	67·4	92·9	95·0	97·6	2·4
5	56·2	68·8	79·0	93·2	94·8	97·8	2·2
6	65·8	76·7	85·5	95·6	97·2	99·4	0·6
Shatter Tests on 1-in. Material (Water Cooled)							
1	29·6	51·2	72·4	92·8	95·2	98·0	2·0
2	37·1	53·4	72·4	92·6	95·2	98·2	1·8
3	18·3	45·8	71·2	92·5	95·0	97·9	2·1
4	Not sufficient material						
5	16·0	33·8	54·6	87·7	92·3	98·2	1·8
6	57·8	75·4	83·6	94·2	96·4	98·8	1·2

* **The 50 lb. of +1-in. sinter required for the standard coke shatter test was made up in the aliquot proportions indicated by the screen analysis**

Table V

EFFECTS OF HANDLING, TRANSPORTATION, ETC., ON THREE TYPES OF SINTER

		+4 in.	+2 in.	+1 in.	+¾ in.	+½ in.	+¼ in.	+⅛ in.	+1/16 in.	−1/16 in.
Normal sinter, −⅝-in. ore fines, ex wagon*	water	...	5·0	21·8	34·6	54·0	86·6	90·0	95·8	4·2
Normal sinter, −⅝-in. ore fines, ex furnace†	cooled	...	1·1	2·3	12·2	35·4	78·0	91·2	96·1	3·9
Simplex ⅝-in. sinter, ¼-in. fines, ex wagon	air	18·3	33·5	50·3	59·7	74·3	93·9	96·3	99·0	1·0
Simplex ⅝-in. sinter, ¼-in. fines, ex furnace	cooled	...	2·0	11·7	20·1	35·9	75·4	81·8	92·0	8·0
Duplex sinter, ex wagon	water	14·4	27·2	45·6	53·7	70·8	91·4	95·7	98·1	1·9
Duplex sinter, ex furnace	cooled	...	8·9	20·2	33·7	48·7	80·7	92·9	96·8	3·2

* **' Ex wagon ' is material sampled as it was loaded into wagons from the discharge end of the sintering machine**

† **' Ex furnace ' is the same material after passing through the furnace bunkers and sampled as it was being discharged into the furnace skip**

semi-raw centre, in contrast to rich ore, which has a very low loss on ignition, and therefore is not usually a source of dusting.

If the calcined piece passes through the final screens and goes back as return fines, trouble arises, as will be shown later.

The effect of free lime is similar. The greater part of the lime present in Frodingham ironstone is there as calcite in the form of fossil shells. When a $\frac{1}{2}$-in. piece of shell passes along the sintering machine, CO_2 is lost and the shell passes off the end of the machine as a piece of free CaO surrounded by sinter. The lime is another major cause of dusting because in the presence of water it swells and may cause fracture of the sinter piece, as shown in Fig. 6.

It was obvious that there was an upper limit to the particle size of Frodingham ore which could be sintered successfully in order to avoid calcined pieces, and to bring about fixation of CaO. After considerable experiment, it was shown that providing other conditions—especially in regard to return fines—could be fulfilled, all Frodingham ore for sintering must be smaller than $\frac{1}{4}$ in. Although considerable improvement in sinter strength resulted from using $-\frac{1}{4}$-in material, there was little further benefit in using smaller material, and certain operating difficulties presented themselves.

Northampton Ironstone Fines—As it had already been decided that all Northampton ore was to be dried, the natural fines may be disregarded.

Dried Northants ore screened through $\frac{3}{8}$-in. or $\frac{1}{2}$-in. mesh was usually considered satisfactory sintering material. However, since future practice would include screening at $\frac{5}{8}$ in., to improve the ore burden of the blast-furnace, experiments were carried out to determine the behaviour of $-\frac{5}{8}$-in. dried fines as a sintering material.

A typical screen analysis is :

$+\frac{3}{4}$ in.	$+\frac{1}{2}$ in.	$+\frac{1}{4}$ in.	$+\frac{1}{8}$ in.	$+\frac{1}{16}$ in.	+16 m.
0·2	1·0	18·1	54·1	69·9	73·7
+30 m.	+60 m.	+100 m.	−100 m.		
80·0	87·1	93·0	7·0		

and a typical chemical analysis, in percentage, is :

H_2O	FeO	Fe_2O_3	CaO	SiO_2	Al_2O_3
10·2	5·0	46·70	3·0	16·7	6·1

MgO	S	MnO	Sintering Loss	Total Fe
1·53	0·081	0·16	24·5	36·6

There is no free $CaCO_3$ in this ore but the loss on ignition is of the same order as for Frodingham fines. Test-runs quickly showed that pieces near the maximum size (*i.e.*, about $\frac{1}{2}$ in.) behaved in the same way as the larger pieces of Frodingham fines and did not sinter. In the sinter, such pieces caused dusting and mechanical weakness and in the return fines, caused mixing trouble. Test-runs similar to those used for Frodingham fines demonstrated that the Northampton fines must be $-\frac{1}{4}$ in. for good sinter. The effect on sinter quality of using $-\frac{1}{4}$-in. Frodingham and Northants fines is shown in Table VI. Apart from the influence of maximum particle size on the actual sintering operation, it was also demonstrated that reduction to $-\frac{1}{4}$ in. had beneficial effects on feeder-table variation, size segregation, and mixing.

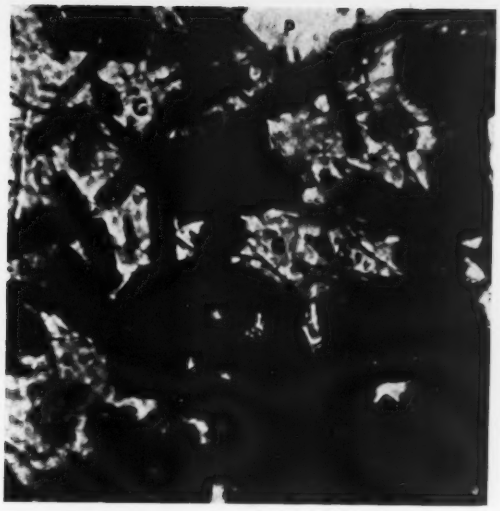
As a result of these tests (and others carried out in the screen house and on the blast-furnace) it was

Table VI

SCREEN ANALYSES OF VARIOUS TYPES OF SINTER

No.	Type of Product	Raw Materials	Sampling Point	Method of Cooling	+ 1 in.	+ $\frac{3}{4}$ in.	+ $\frac{1}{2}$ in.	+ $\frac{1}{4}$ in.	+ $\frac{1}{8}$ in.	+ $\frac{1}{16}$ in.
1	Normal	All − $\frac{5}{8}$-in. ore fines with normal return fines	Wagon below discharge chute of machine	Air	23·7 (43·1)*	35·9 (56·3)	62·6 (74·4)	85·1 (92·7)	89·2 (95·1)	97·3 (98·0)
2	Normal	As 1	As 1	Water	21·8 (30·7)	34·6 (50·9)	54·0 (70·5)	86·6 (92·5)	90·0 (94·6)	95·8 (97·6)
3	Normal	As 1	Between furnace bunkers and skip	Water	2·3 (30·2)	12·2 (51·8)	35·4 (73·8)	78·0 (91·0)	91·2 (94·0)	96·1 (98·8)
4	Experimental	All ore fines hammer-milled to − $\frac{1}{4}$ in. with normal return fines	As 1	Air	36·6 (49·2)	47·3 (60·4)	71·5 (75·2)	92·4 (92·8)	95·4 (94·8)	98·4 (97·8)
5	Experimental	Ore fines hammer-milled to − $\frac{1}{4}$ in. with 40% unscreened stock sinter	As 1	Air	46·0 (46·8)	60·3 (62·0)	76·8 (76·7)	94·5 (93·1)	96·2 (95·3)	98·5 (98·3)
6	Experimental	All ore fines − $\frac{5}{8}$ in. with 40% unscreened stock sinter	As 1	Air	20·4 (30·5)	31·2 (46·0)	57·3 (62·4)	92·3 (88·3)	96·1 (94·4)	98·5 (98·0)
7	Experimental	All ore fines hammer-milled to − $\frac{1}{4}$ in. with 40% − $\frac{1}{16}$ in. sinter	As 1	Air	50·3 (54·5)	59·7 (67·0)	74·3 (78·6)	93·9 (92·6)	96·3 (95·0)	99·0 (98·0)
8	Experimental	As 7	As 3	Air	11·7 (46·0)	20·1 (64·6)	35·9 (79·2)	75·4 (94·0)	81·8 (95·9)	92·0 (98·1)
9	Experimental	Re-sintered sinter (Duplex)	As 1	Air	64·3 (67·6)	73·7 (77·8)	84·0 (86·0)	96·5 (95·3)	98·2 (96·8)	99·4 (98·7)
10	Experimental	As 9	As 1	Water	45·6 (54·4)	53·7 (68·4)	70·8 (79·4)	91·4 (92·7)	95·7 (95·5)	98·1 (98·3)
11	Experimental	As 9	As 3	Water	20·2 (59·0)	33·7 (69·5)	48·7 (78·1)	80·7 (92·1)	92·9 (94·9)	96·8 (98·0)

* Figures in brackets represent the shatter test results using + 1-in. material



F = Fayalite
G = Glass
P = Pore-space
Black areas = Magnetite

Fig. 4—Typical micro-section of Appleby-Frodingham sinter (transmitted light) × 600

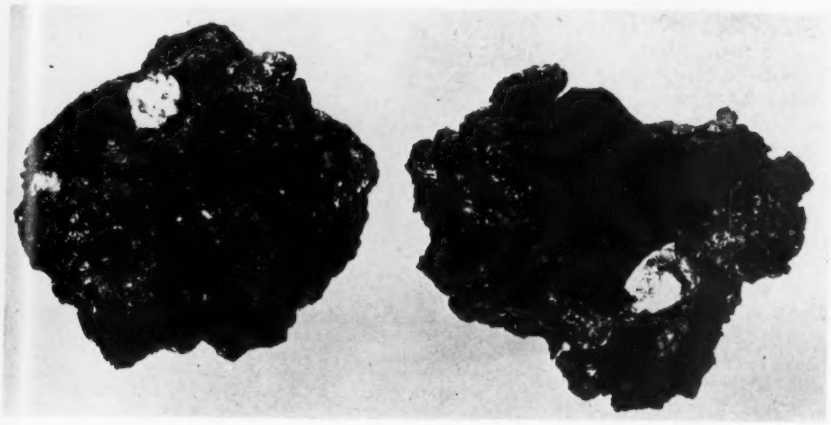


Fig. 5—Sinter showing included pieces of CaO and calcined ore × approx. ½

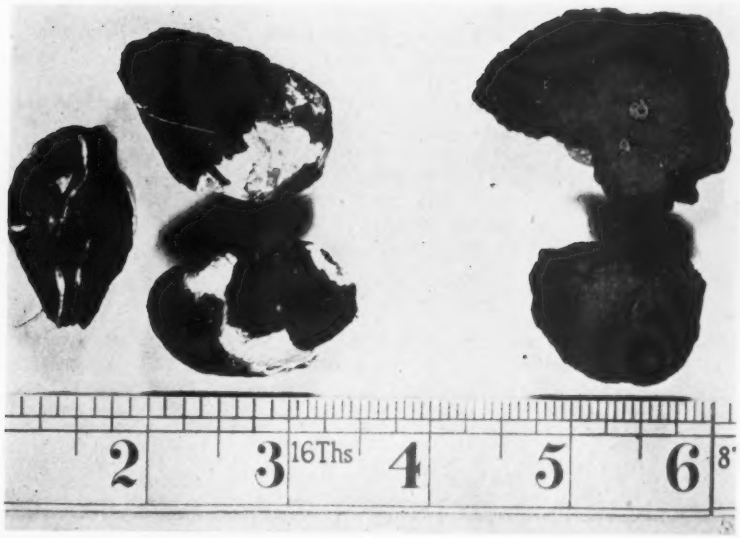


Fig. 6—Unsatisfactory return fines showing that CaO retains its fossil shape. The zoning of the ore pieces indicates the progress of calcination × ⅞

decided that on the new plant no ore smaller than $\frac{5}{8}$ in. should go to the blast-furnace and that no ore larger than $\frac{1}{4}$ in. should go to the sinter plant.

Many investigations at plant scale had shown that it was still essential to charge a clean ore burden to the furnace ; in other words, sinter quality must not be improved at the expense of the remainder of the burden.

All factors indicated crushing of the screened-out fines. This was a serious problem and the final solution involved a long and difficult plant research into screening and tertiary crushing.

Return Fines

This subject proved to be the most important one of all. The historical origins of return fines are not mentioned in the literature ; indeed, the subject of return fines is almost ignored although in every sintering process at least 20% of the raw material fed to the machine or pan is return fines.

It is believed that return fines originate in two ways :

(*a*) As rejected material because of the inefficiencies of the sintering process, and (*b*) as a deliberate re-circulation of good product in order to assist sintering.

Whatever the origin, there is no doubt that both factors necessitate the use of return fines in the sintering process. There is also no doubt that return fines are a mixture of incompletely sintered material (arising from inefficiencies of sintering and limitations imposed by raw materials) and of good sinter (arising from the inability of the final screen to distinguish between sintered and unsintered material). In practice there are three types of return fines :

(*a*) Raw or dried material which falls through the sintering grate or is spilled around the feed end. This type may also result because of a local shortage of coke

(*b*) Calcined material, usually because particle size is too large for sintering or because of a more confined local shortage of coke than in (*a*). Both points add further emphasis to the importance of adequate mixing and the dangers of size segregation

(*c*) Good sinter returned *via* the final screens. (The experiments showed that sinter extracted from return fines and sized at $\frac{1}{4}$ in. to $\frac{5}{8}$ in. was stronger than any other size fractions, as measured by a modified shatter test.)

It will be conceded that the amounts of types (*a*) and (*b*) returned may be largely fortuitous, and that the amount of type (*c*) returned will be a function of sinter quality. This is true only if the final screens are not overloaded by an excess quantity of return fines types (*a*) and (*b*). Further, the stronger the sinter being made, the less of type (*c*) will be returned.

It was suggested during discussions of the experimental work that if return fines were reduced to zero, sinter quality would be little affected, and to prove this, sinter was made entirely from ' raw materials '—the feed of return fines being stopped until the return fines bins were completely filled. The results indicated that the sinter produced under such conditions was little different from the normal product. At the conclusion of the test, the return fines bins were filled to the screen bars and, to restore normal conditions as quickly as possible, the contents of the return fines bins were sintered with about 6% of coke but with no other raw material ; the product was the best sinter so far made.

Duplex Sinter—A full-scale test was made in which sinter was returned to the plant in wagons and re-sintered. The product—Duplex sinter—confirmed that a highly satisfactory material could be produced. The test was followed by one in which 6000 tons of sinter from stock were re-sintered and used under test conditions in one of the Company's 22-ft. hearth dia. blast-furnaces. The output of pig iron increased by 15% and the coke consumption decreased by 7% compared with figures obtained two weeks before and two weeks after the trial. Corrections were made for other variables, and the improvement may therefore be regarded as a reasonable indication of the effect of sinter quality on blast-furnace operation.

If judged by usual standards, this Duplex sinter was too heavily slagged to be considered good. Its reducibility, as measured in the laboratory, was inferior to any other sinter. But it was clean and strong, and acceptable to the blast-furnace (*see* Table VI).

During normal sinter production, about 35% of return fines is extracted by the final screens. On almost all plants, including the South Works plant, the accurate quantitative measurement of return fines is a very difficult matter. During the production of Duplex sinter, return fines, with no alteration to the screening arrangements, dropped to below 10%. In all the tests, it was found that as sinter quality improved, the amount of return fines produced decreased until there were insufficient return fines to maintain sinter quality (except when duplexing). The ensuing deterioration then led to an increase in the amount of return fines, but as these were of much poorer quality, sinter did not quickly return to satisfactory quality. It became more and more obvious that the quality of return fines was as important as the quantity. Because of the low surge capacity of the return fines bin, the process has to consume the return fines it is making. Such conditions can only lead to unsteady operation since the cyclic effect is usually uneven. Therefore, if all other things are unchanged, the mixture at the discharge end of the machine differs from the mixture at the feed end.

In practice, provided that there is reasonable surge capacity in the return-fines flow, this is not necessarily a serious trouble, particularly with good raw materials for which the quality of return fines varies only slightly. In the sintering of British ore the quality of return fines can, and does, vary considerably ; calcined stone returned to the process has not the same effects as good sinter returned. Every variation in sintering makes itself felt in variations in the quality and quantity of return fines. If coke is too low soft sintering leads to the screening out of a large quantity of soft sinter ; if coke is too high local slagging, with consequent ' sealing off ' of parts of the bed, leads to the screening out of large quantities of raw and calcined ore fines, with little hard, slaggy sinter returning to the process. It requires several circuits of a machine to eliminate the effects of really bad sinter and to restore the return fines to satisfactory and regular quality and quantity.

Duplexing avoided the need for correction, by plant additions and alterations, of all the factors so far enumerated, but it would obviously be a most

expensive matter to double the plant and obtain very little increase in output.

Simplex Sinter—A series of plant experiments was conducted using prepared raw materials (ore fines crushed to $-\frac{1}{4}$ in.) and introducing sinter from stock *via* the raw material bunkers. The sinter returned was the normal product which had been in the stock-yard. At first no attempt was made to size this material, and some trouble with irregular feed from the tables resulted from the wide size ranging. It was found that when adding 40% of stock sinter, a product approaching the quality of Duplex sinter was obtained. As equilibrium was reached, the return fines from the process fell from 20% to 8%. This practice was called ' simplexing.'

Further tests were made using stock sinter screened at $-\frac{5}{16}$ in. When the process was operating on its own return fines, the mixture, which gave a product as good as Duplex, was as follows :

41·5% stock sinter, $-\frac{5}{16}$ in.
32·5% Frodingham fines, $-\frac{1}{4}$ in.
18·0% Northants fines, $-\frac{1}{4}$ in.
8·0% coke, $-\frac{1}{8}$ in.
8·5% return fines, -1 in.

Whilst maintaining a constant feed of $-\frac{5}{16}$-in. sinter, the ore fines were changed to the normal $-\frac{5}{8}$-in. material, and this brought about immediate deterioration in quality of product. In an attempt to correct matters the amount of stock sinter was increased, but as return fines also increased (to 23% in less than an hour after the change in ore fines) quality could not be restored to its former levels.

This test, checked several times, was perhaps the most important of the hundreds carried out. Unless duplexing was to be adopted, raw materials had to be reduced in size to $-\frac{1}{4}$ in., even when a large proportion of sinter was being returned as sinter not, as return fines.

Return Sinter—It was essential to size the returned sinter if the best results were to be obtained ; the need for reducing the ore fines to $-\frac{1}{4}$ in. had already been proven. The object of adding sinter was largely defeated by using unsized sinter with this material because it often led to excessive segregation so that the lower part of the bed was rich in sinter whilst the upper part was deficient. Experiment indicated that a maximum size of sinter of $\frac{1}{2}$ in. was quite satisfactory. Good sinter can be made only when return fines are of good quality and good return fines can be made, with the ores under discussion, only when raw materials are small enough to be efficiently sintered. It must be emphasized that return fines must be return sinter ; not just rubbish. It was found that returned sinter improved the strength of the final product whereas returned raw or calcined material did not. Table VII shows variations in return fines as measured by the proportions of magnetic and non-magnetic constituents.

In routine practice it was beneficial to ' purge ' the whole plant system about every 12 hr. All raw material feed, except coke, was stopped and the contents of the return fines bins were re-sintered. This resulted in improved quality, and thus a reduced amount of return fines and permitted the use of rather higher strand speeds, with consequent improvement in output. The amount of unsintered material in the return fines tended to increase with prolonged operation at high speeds, and tests confirmed that much of the larger ore fines had to circulate three or four times before being sintered.

Modifications to New Plant—In the new installation it was decided to provide for the following :

(*a*) Air cooling of the sinter after leaving the machine
(*b*) Screening of the sinter after cooling
(*c*) Crushing of the sinter so extracted before returning to the system *via* raw-material bins
(*d*) Controlled variations of sinter screening and crushing to permit the return of any desired percentage of sinter, to meet any variations in quality which may arise
(*e*) Very generous surge capacity for return fines
(*f*) Addition of return fines to the system as a raw material at the discretion of the operator, and not as the operation dictates at the time
(*g*) Generous mixing capacity, including pre-mixing and pre-watering of return fines in order to overcome as quickly as possible the effects of calcined and semi-sintered fines.

The final installation will be such that, for several hours, it will be possible to process either raw fines, return fines, or sinter, each alone or in any combination of quantity with either or both of the remaining two materials.

PLANT CONTROL

Throughout the work, it was apparent that very

Table VII

PROPORTIONS OF SINTERED AND UNSINTERED MATERIAL IN RETURN FINES FROM VARIOUS SINTERING MIXTURES

	$\frac{3}{4}$-in. Fraction		$\frac{1}{2}$-in. Fraction		$\frac{1}{4}$-in. Fraction		$\frac{1}{8}$-in. Fraction		$\frac{1}{16}$-in. Fraction		$-\frac{1}{16}$-in. Fraction	
	Sintered, %	Unsintered, %	Sintered, %	Unsintered, %	Sintered, %	Unsintered, %	Sintered, %	Unsintered, %	Sintered, %	Unsintered, %	Sintered, %	Unsintered, %
Normal returns	84	16	85	15	82	18	57	43	49	51	52	48
Hammer-mill returns	100	...	99	1	95	5	73	27	64	36	36	64
Simplex returns	100	...	100	...	95	5	90	10	82	18	71	29

The separation between the sintered and unsintered material was effected by hand picking the larger pieces and then carrying out a magnetic separation. The non-magnetic fraction is considered to be unsintered material

close operational control of the process was essential. This meant that more and more controls had to be centralized in one place, and this in turn placed great emphasis on instrumentation and communications. It was therefore decided to provide extensive instrumentation and means for inter-communication in the new plant installation. Figures 7 and 8 show the layout of the instrument panel and control desk which are being installed.

OTHER PRACTICE DETAILS

Much work was done on such subjects as suction, windbox temperature, bed permeability, and so on, but if the more important items already mentioned are controlled, these other factors will look after themselves. It has long been established that coke breeze for sintering should be $-\frac{1}{8}$ in. in size, and this still holds true for the projected sintering practice. The amount of breeze needed is a matter of routine operation ; if too little is added, soft sinter and a large amount of poor-quality return fines will result, and if too much is used, slagging of the bed will occur. The range between these two extremes dictates the amount of coke to be used.

PLANT MAINTENANCE

The standard of maintenance on a sinter plant must be of a very high order. In few other types of plant does skimped maintenance so quickly produce a high maintenance bill, or does neglected maintenance give rise to such production difficulties. Most maintenance faults are reflected sooner or later in the return-fines practice. Holes and gaps in the grate bars mean more calcined and raw fines going back as returns ; permitting the gap at the end of the machine to get too wide leads to more return fines and to damaged pallets and greatly increased abrasion of the fan impellor ; belt conveyors incorrectly trained lead to segregation of materials ; dirty swinging spouts lead to irregular feed on the machines ; worn screen bars lead to too large material going back, with consequent mixing and segregation difficulties ; neglected pug mills and mixers lead to inefficient mixing ; worn fan impellors lead to lower outputs. There is hardly a piece of equipment around a sintering plant on which neglect does not react on sinter quality.

As a result of experience and plant research, careful consideration was given to maintenance in designing the new plant. To permit easy control of the gap the new machines will be fitted with adjustable discharge ends. Generous dust extractors will be fitted to protect the fan impellors from abrasion. All mixers will be installed in duplicate so that mixer maintenance will affect production as little as possible. Experience has shown that plant cleanliness and sinter quality are closely related, so that everything possible will be done to ensure a clean plant, even to the extent of fitting dust-extraction equipment to the discharge end of the machines.

Acknowledgments

The foregoing paper is based on the very voluminous reports accumulated in carrying out a large-scale operational research. The research occupied a fairly large team of workers for over a year and a smaller team for another year and a half. As all the work was at plant scale, it followed that everybody working on the ore-preparation plant automatically became a member of a research group at some time or other. To mention, by name, everybody who helped would be an impossible task, as almost every section of the ironworks and associated departments was involved at one time or other.

The authors are grateful to A. English, N. Furnival, and L. A. W. Paynter for their important contributions ; to the Ironworks Engineering staff, especially J. H. Barker, A. Bridge, W. G. Crooke, S. Goodson, C. Orwin, and W. Richardson, who worked hard and successfully to meet arising mechanical problems during the investigation ; to D. R. M. Nisbet and F. Pike, who did excellent work in solving electrical

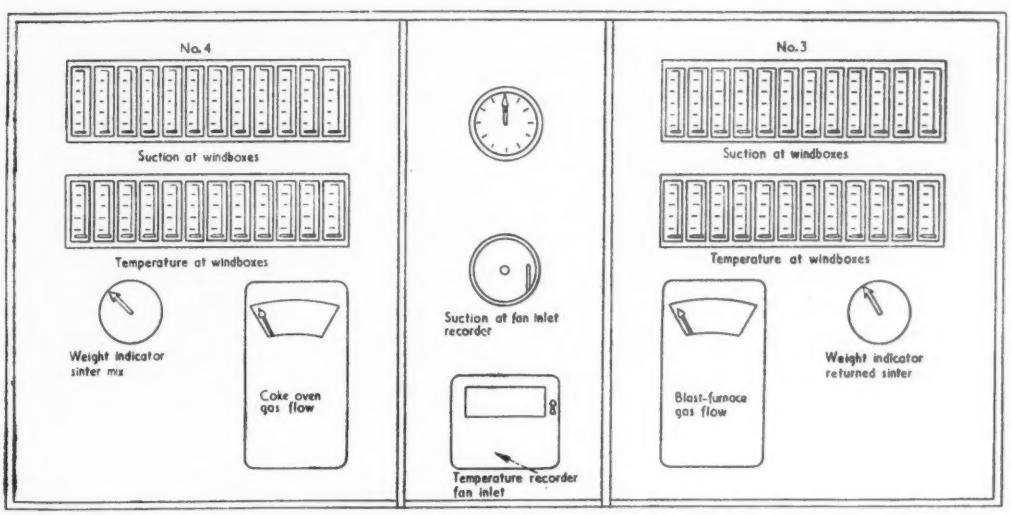


Fig. 7—Sinter plant instrument panel (machines 3 and 4)

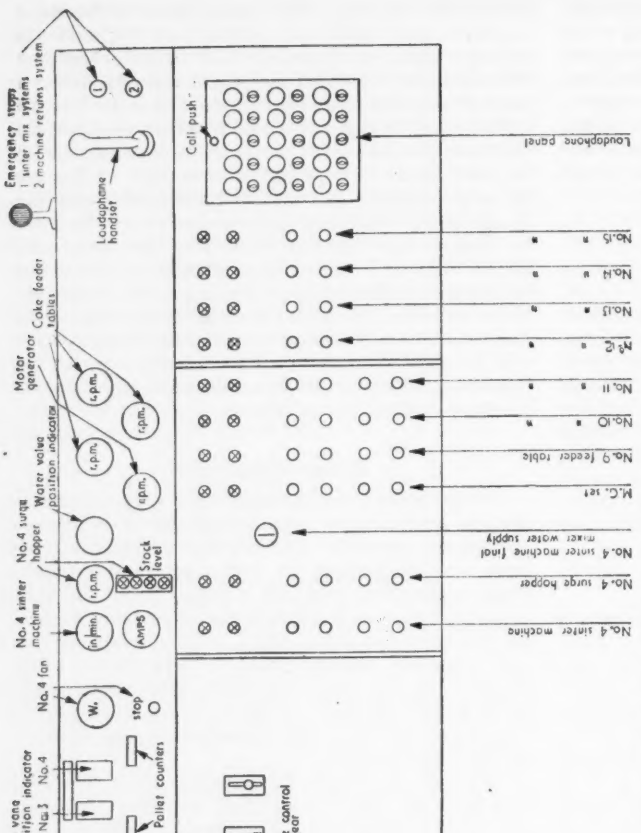


Fig. 8—Sinter plant control desk (machines 3 and 4)

problems, especially those of control and communication.

F. Dunnill, of the design staff, made satisfactory designs of many plant alterations and additions needed during the investigation. Especially are the authors grateful to him for the idea and design of the revolving distributor in the surge hopper.

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The Evolution of the All-Carbon Blast-Furnace

By J. H. Chesters, G. D. Elliott, and J. Mackenzie

SYNOPSIS

The use of carbon bricks in the blast-furnace is traced from the early hearth experiments, designed to minimize breakouts, to the present stage of three all-carbon blast-furnaces at Appleby-Frodingham. The properties of carbon bricks and the essential design features of carbon hearths are discussed in detail. It is concluded from operating evidence and from the results of examination of blown-out hearths that carbon is successful in preventing breakouts.

Trials of carbon bricks in bosh and stack are described with particular reference to scaffold formation. Operation and cost details of No. 6 furnace, Appleby-Frodingham, which was built as the first blast-furnace in the world completely lined in carbon and was blown-in in April, 1949, and of No. 1 and No. 5 furnaces, which were later relined in carbon and blown-in in October, 1949, and April, 1950, respectively, are discussed. In spite of burden handicaps, in the first year of operation No. 6 furnace made 12%, and No. 1 furnace 4%, more iron than in the first year of the previous campaigns.

Introduction

DURING the last ten years many trials have been carried out at the Appleby-Frodingham branch of The United Steel Companies, Ltd., on the use of carbon bricks for blast-furnace linings. As a result, three furnaces completely lined with carbon from top to bottom are now in operation. Much new information has come to light during these experiments, and it seems desirable, therefore, to trace the history of the development, and to review the ideas on carbon bricks and the design features considered important in the use of carbon.

As a result of an extensive study of breakouts reported in The Iron and Steel Institute *Special Report No.* 30 (1944),[1] the first trials of carbon bricks in the hearth were made. The position has now been reached when, out of eight furnaces at Appleby-Frodingham and three at Workington, carbon is being used for the hearths on all but one. The problem of scaffolds has also been examined, and several ideas of prevention by means of carbon bricks were reported in 1947.[2]

During this period a great deal of attention was naturally being given to the development of the carbon brick and to increasing the knowledge of carbon and its behaviour in the blast-furnace.

Prior to 1941 it was the practice of The United Steel Companies, Ltd., in common with other operators, to use a good high-alumina fireclay brick in the hearth and bosh and a hard medium-alumina brick in the stack. Between 1929 and 1943, 23 hearth breakouts occurred and there were many large and troublesome scaffolds. In the past eight years, apart from a breakthrough in No. 5 furnace, which, as reported at the time,[3] was due to special circumstances in the design of the hearth and in the foundations of the furnace, there has not been a breakout in any furnace of The United Steel Companies, Ltd., which uses carbon in the hearth. Up to July, 1950, when No. 5 furnace broke out, three million tons of iron had been made with no sign of a breakout, whereas the three million tons made on fireclay prior to the change to carbon were accompanied by 14 breakouts. Although it is early to make any claims for the stack, there is no evidence of scaffold formation on the carbon stacks or on any of the carbon panels that have been tried in the stack.

When carbon was taken up seriously it was, of course, by no means a new refractory. Carbon ramming had been used in Britain since before 1900, and has been used at Appleby-Frodingham since 1905 for such purposes as lining runners, and as a filling between the hearth jacket and fireclay linings. One or two unsuccessful experiments have been made using this material as part of the actual hearth lining, but the linings failed because of the formation of large cracks on firing. It appears that this shrinkage cracking will always be present with rammed carbon, giving rise to heavy metal penetration early in the life of the hearth. There was, also, considerable German experience with rammed carbon and the use of large blocks in hearth construction.

Research began with an investigation of the best method of preparation of carbon ramming, and the examination of various coke tar mixtures. As this work progressed, it was extended to see whether preformed carbon refractories, with properties which in the light of knowledge and experience of blast-furnace refractories were considered satisfactory, could be manufactured with a view to use in the furnace. The first three carbon test pieces made from experimental batches in August, 1941, are shown in Fig. 1. These led to the first test bricks, which were found to have properties superior to those given in the published data for Continental bricks. In February, 1942, 1000 of these experimental 9-in. squares were made at a co-operating brickworks. Some were used in runners, and some were built into the top of the hearth of No. 7 furnace, Appleby-Frodingham; there is no reason to doubt that they are still there. The remaining bricks were used in patching a serious breakout at No. 9 furnace and in arresting a breakout known

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Dr. J. H. Chesters and Mr. J. Mackenzie are the Assistant Director of Research and the Head of Refractories Section, respectively, of The United Steel Companies, Ltd., Sheffield. Mr. G. D. Elliot is Blast-Furnace Superintendent of the Appleby-Frodingham Steel Company branch of The United Steel Companies, Ltd.

to be imminent at No. 10 furnace. No. 9 furnace was blown out three years later and the carbon bricks were found to be completely intact, although as little as 4½ in. of carbon separated the liquid metal from the cooling plate.

PROPERTIES OF CARBON AND FIRECLAY REFRACTORIES

It is perhaps desirable to compare some of the laboratory properties of carbon and fireclay bricks. All the figures given are typical averages of results obtained in the laboratories of The United Steel Companies, Ltd., in testing deliveries of refractories for blast-furnace lining construction. It has also been found necessary to examine properties which have never been evaluated before, and some of these data are presented. It is, of course, well known that the properties of blast-furnace fireclay refractories have considerably improved in recent years : the data given below were obtained on deliveries made several years ago, when carbon bricks were first used in hearth construction.

Standard Refractory Tests

Refractory tests of bricks of carbon and fireclay have given the following results :

	Carbon Brick	Fireclay Brick
Apparent porosity, %	22·3	23·9
Bulk density, g./c.c.	1·51	2·01
Specific gravity (by porosity)	1·95	2·64
Permeability, c.g.s. units	0·022	0·160
After-contraction, linear change (2 hr. 1500° C.)	Up to 1%	Up to 1%

Very few refractory technicians are satisfied with the after-contraction test for fireclay, in view of the numerous errors that are likely to influence the final result. Fireclay, by its very nature and mineralogical make-up, shrinks when heated, and the amount of this shrinkage is dependent, among other things, on two important factors, time and temperature. Consequently, if a fireclay brick is heated for any length of time at a temperature greater than the kiln-firing temperature, as it is in the hot zones of the blast-furnace, the natural shrinkage of the clay will continue; it is not uncommon to remove from a furnace hearth bricks with almost zero porosity, which, assuming that the original material had a porosity of 20–25%, corresponds to a shrinkage of about 8%.

With carbon no such natural shrinkage occurs, and the result is a very high volume stability. Even at 2000° C. a sample of carbon brick showed a contraction of only 2·7%. A slight shrinkage within a carbon brick is to be expected at higher temperatures, because of the tendency to form graphite which, with a specific gravity of 2·25 compared with 1·95 for the original brick, involves a decrease in volume.

The following figures show that the refractoriness-under-load of carbon is very much superior to that of even the best of fireclay bricks :

	Carbon Brick	Fireclay Brick
Melting point, ° C.	>3000	1740
Refractoriness-under-load :		
50 lb./sq. in.	No deformation at 1700° C.	Rapid softening at 1500° C. Failure at 1600° C.
2 tons/sq. in.	No deformation at 1470° C.	—

Under a load of 50 lb./sq. in. no deformation has been noted in carbon specimens at temperatures in excess of those attained in blast-furnace operation.

Physical and Chemical Tests

Results obtained for various physical and chemical tests were as follows :

		Carbon Brick	Fireclay Brick
Adhesion of iron ores		None of the ores tested showed adhesion	All ores tested showed adhesion and beginning of slagging
Carbon monoxide disintegration		Very little risk	Considerable risk
Chemical attack		Inert to iron, slag, and alkalies	Reacts with iron, slag, and alkalies
Electrical resistivity, ohms × 10^{-3}/c.c.		5·9	...
Reaction with carbon dioxide		Takes place above 700° C.; rapid above 1000° C.	None
Reaction with oxygen		Rapid above 400° C.	None
Thermal conductivity, B.Th.U./sq. ft./hr./° F./in.			
Mean temperature	300° C.	15·9	5·1
	600° C.	20·6	No comparable data available
,, ,,	800° C.	24·0	,, ,,
,, ,,	1000° C.	27·2	,, ,,
Thermal shock resistance, No. of reversals		30+	30+
Thermal expansion (20–1000° C.), %		0·65	0·55

These figures show that carbon brick has a thermal conductivity approximately three times that of fireclay brick, a factor which will be of considerable significance in the efficiency of external water cooling of a blast-furnace lining.

Wetting by Iron and Slag—Whereas molten slag and metal will wet fireclay bricks relatively easily, carbon under normal conditions possesses the property of resisting this action. In this respect, slag or metal on carbon in many ways resembles mercury running over a wooden surface. This non-wetting property of carbon has proved of value in several applications. For example, when carbon bricks were tried as a replacement of sand in iron casting runners, it was noted that very little scrap was left in the runner after a cast, and that what little skull that did appear was always loose and could be easily and cleanly removed. It is important also to note that both mechanical and chemical erosion of a refractory are less severe if it is not wetted by the liquid with which it is in contact.

Mechanical Tests

Abradability—A large amount of data on the resistance to abrasion of various carbon and fireclay blast-furnace refractories has been collected, using a steel shot-blast technique.[4] The information shows that the resistance to abrasion of the present-day carbon brick is better than that of the average hearth quality fireclay or of the harder stack quality fireclay brick. In one series of tests, carbon bricks were shown

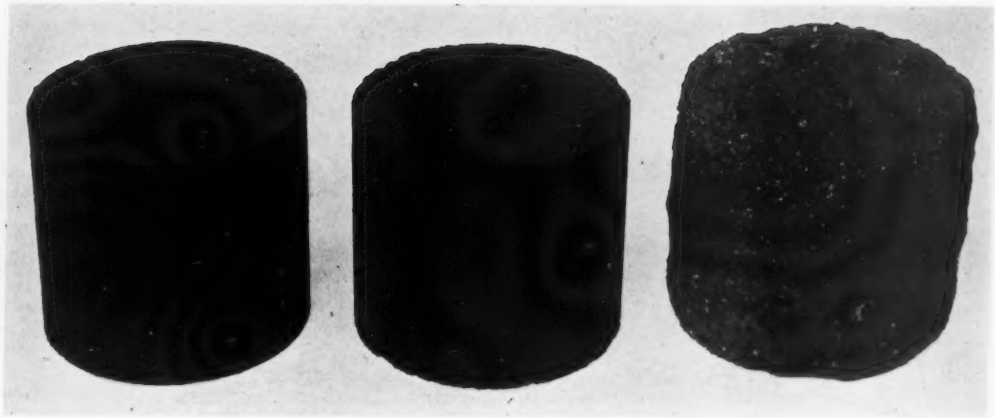

Fig. 1—Coke-tar carbon bricks made with fine, medium, and coarse coke

Fig. 2—Carbon bricks in blown-out hearth of No. 10 furnace, floating above original hearth level

Fig. 3—Section of blown-out hearth of No. 10 furnace, sampled for laboratory examination

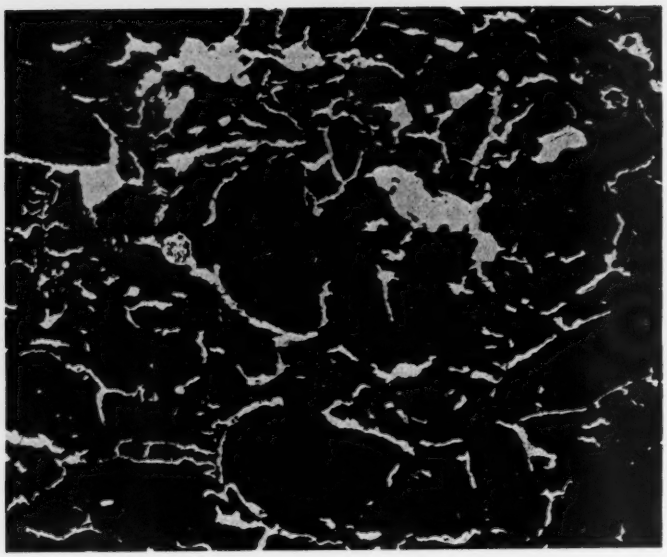

Fig. 4—Polished section of fireclay brick from No. 10 furnace hearth, showing metal impregnation

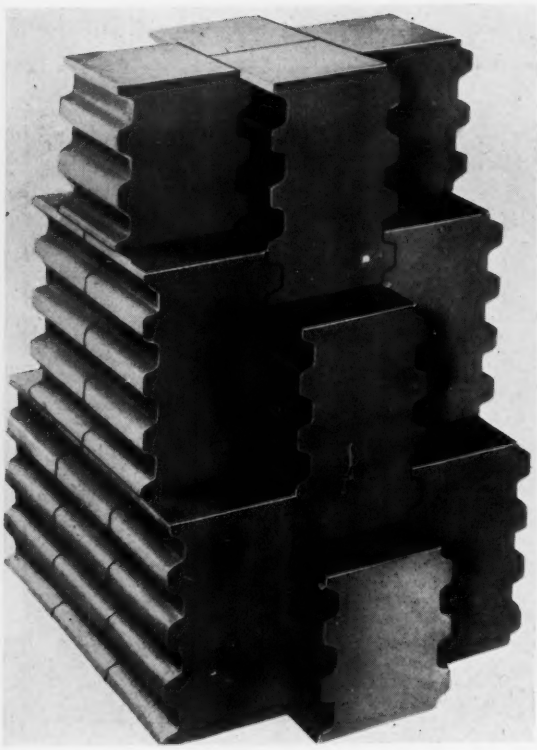

Fig. 5—Corrugated carbon block used in the latest blast-furnace bottoms. (Registered design)

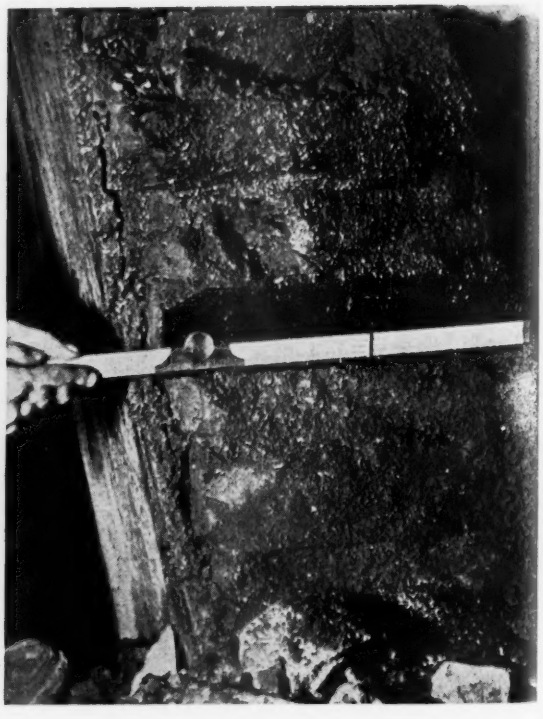

Fig. 6—Carbon bosh in No. 1 furnace, Appleby-Frodingham, after 4 years' service

to be almost twice as good as a normal run of fireclay bricks.

Cold Crushing—There is no doubt that the cold-crushing values for fireclay bricks have been improving in recent years, but it is still found that the average value for fireclay material is below the lowest figure recorded in quality control testing in the manufacture of carbon bricks. Statistical data have shown that the average figure for carbon bricks is 9800 lb./sq. in.:

	Carbon Brick	Fireclay Brick
Cold crushing strength, lb./sq. in.:		
Range	6300–15,000	1100–6050
Average	9800	2100

Several other strength measurements had to be made in the course of calculations on the stability of various hearth designs. It may be of interest to record these values:

lb./sq. in.	Carbon Brick	Fireclay Brick
Modulus of rigidity	$9{\cdot}3 \times 10^5$	$12{\cdot}2 \times 10^5$
Modulus of elasticity	$1{\cdot}9 \times 10^6$	...
Shear strength	2390	850
Tensile strength	1450	700
Transverse breaking strength	2420	1000

CARBON IN THE HEARTH

In the early nineteen-forties, breakouts through fireclay hearths were an acute problem. After much laboratory work on samples of blown-out hearths, a theory on breakouts through fireclay hearths in addition to that of excessive vitrification and shrinkage was evolved.[1] This theory, which has since been confirmed by many later investigations, is based on the formation, behind the working face, of a zone rich in FeO, from the oxidation of penetrating iron by infiltrating air, CO_2, or water vapour. This FeO zone would slag the fireclay refractory, and would cause the working face above to be washed away. Such a process would be continuous and would result, it is believed, in a breakout in the area of maximum activity.

At this point, the weight of evidence—the breakout theory, the favourable properties of carbon refractories, the successful application of the first trial bricks, and the fact that breakouts were still occurring through fireclay hearths—led to the decision to place the manufacture of carbon bricks on a commercial basis and to instal a complete carbon hearth as soon as possible. An ideal opportunity to do this arose in 1944 with No. 10 furnace. The stack of this furnace was in fairly good condition but the hearth was in poor shape: a complete reline was scheduled for late in 1946, so that an ideal opportunity for a two-year experiment presented itself. The bosh was propped up and the hearth was rebuilt with new cooling plates and British-made carbon bricks.

Carbon Hearth Design

It was decided from the beginning that carbon would be used for two separate purposes in the hearth of a furnace:

(i) To prevent breakouts—these normally occur sideways, so that the main factor involved is sidewall construction

(ii) To prevent salamander formation—as this is a downward effect the hearth floor construction is principally concerned.

It is believed that this division of duty is essential for a proper approach to the problems associated with carbon in hearth construction. Once this division of duty is accepted, hearth floor and hearth wall construction must be considered as two separate items. Experience since the decision was taken has strengthened this belief.

In the general consideration of hearth design, it was obvious that air and water vapour would have to be kept out as far as possible. It is likely, as indicated by the breakout theory, that complete exclusion is impossible, but in an attempt to keep the amount of air and water vapour present to a minimum, the bottom of the hearth was sealed with 16-gauge steel sheets, laid in sections, with a generous overlap. It was customary to cover these sheets with one or two inches of aluminous cement, to give a level base on which to start building a hearth. Recent experience has thrown doubt on the wisdom of using a low melting point cement material below the actual hearth, and it has been decided to omit this, or to substitute, say, coke-tar ramming in future installations.

It is also believed that the low after-contraction of carbon compared with that of fireclay is greatly advantageous in retaining the hearth brickwork in its original position throughout the furnace campaign. Some shrinkage is bound to take place, allowing metal to penetrate into the joints, and in spite of the superior volume stability of carbon the necessity for a good keyed design of hearth, so as to lock each brick in position, even after allowing for maximum after-contraction, is emphasized. With modern manufacturing techniques the 'mould-to-fired' shrinkage of certain carbon bricks is small, thus allowing for greater accuracy of shape, eliminating the need for dressing blocks, and simplifying the assembly of a hearth with thin tight joints.

In considering the design of a carbon hearth, one of the most important properties is the high thermal conductivity of carbon compared with fireclay, which makes for more efficient cooling and therefore prevents excess contraction of the bricks and the penetration of metal through the joints. The question of thermal conductivity applies only to wall construction at the moment, although, to anticipate a little, it is the major factor in the maintenance of the wall. It is considered to be most unorthodox not to utilize this important property of high thermal conductivity and the efficiency of cooling by the introduction of fireclay as a backing between the carbon hearth wall and the water-cooling of the jacket. The destruction of the mechanism of maintaining the wall is one which could lead, and in fact has led, to a disastrous breakout.[5]

The first carbon hearth built in No. 10 furnace had a bottom in the shape of an inverted dome of 55 ft. 7½ in. internal radius, and consisted of two 13½-in. courses of carbon brick sprung off small skewbacks, some carbon and some fireclay. The hearth walls were built in small standard bricks, with two 9-in. courses in front of the stave coolers up to 9 in. below the centre-line of the slag notch. Behind

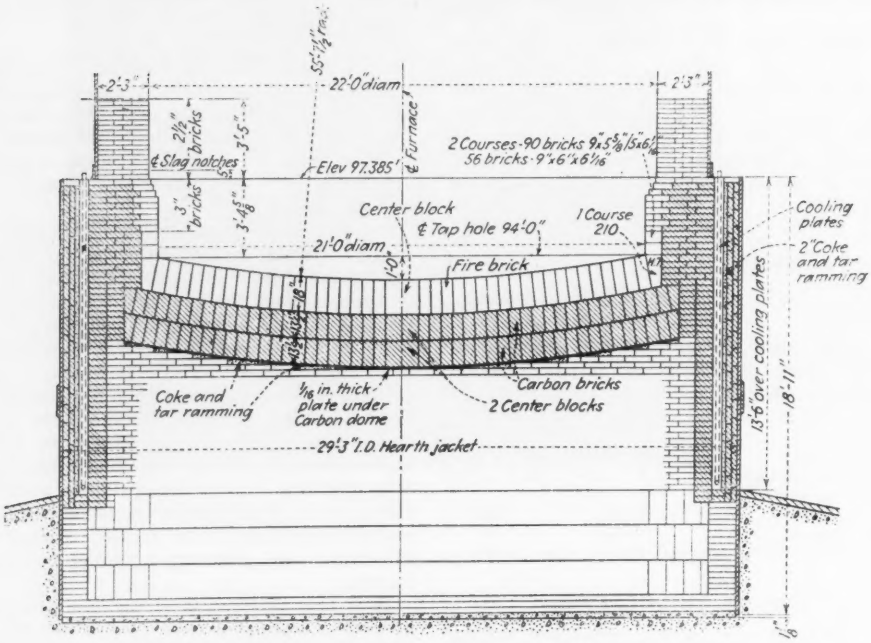

Fig. 7—Construction of carbon hearth built into No. 10 furnace

the staves was a 2-in. layer of coke-tar ramming and then two 3-in. courses of carbon brick to the hearth jacket. It is known that, should metal penetrate to the staves, emergency spray-cooling on the hearth jacket and the thin layer of carbon can be used to hold the hearth. A thickness of 18 in. of fireclay brick was built as a protection for the wall and hearth during blowing-in. A diagram of the construction is shown in Fig. 7.

The furnace was blown-out in February, 1946, after having made slightly more than 200,000 tons of basic iron. Examination showed the walls to be in excellent condition, and that the full thickness of 18 in. of carbon brick was untouched—so much so that the bricks were recovered and used again in a subsequent construction. The 18-in. fireclay protection course in front of the carbon had completely disappeared and had been replaced by a slag/coke conglomerate containing several carbon bricks floating above the original hearth level. This is shown in Fig. 2.

Apart from the first one or two rings, all the carbon bricks of the bottom had disappeared and were replaced by similar material to that found against the walls, together with debris left from the blowing-out. It had been reported that during the campaign various brick-like objects had been observed floating in front of the tuyeres, and there can be little doubt that the complete hearth lifted during its life. During stripping, the section shown in Fig. 3 was exposed, and samples of carbon brick (hearth, wall, and skewback) and fireclay brick were selected for detailed laboratory examination. Figure 3 also illustrates the condition of the hearth walls. The salient features of the results of the laboratory examination are summarized in the following sub-sections.

Carbon Bricks

The slag mass against the carbon wall was detached with ease, confirming the non-wetting nature of the carbon. The floating carbon bricks in the slag mass were slightly rounded at the edges and it appears that these bricks actually formed part of the working face of the hearth, at least during the later stages of the campaign. This shows that the high thermal conductivity of the carbon played a large part in protecting the wall, by allowing the slag to freeze in position and so using this, with floaters from the hearth, as the working face of the wall. Petrological and X-ray examination of this slag showed gehlenite ($2CaO.Al_2O_3.SiO_2$)–akermanite ($2CaO.MgO.2SiO_2$) in large amounts and of large crystal size, suggesting that these crystals had grown in an essentially solid state while being kept for a long time at a temperature sufficiently high for crystallization to proceed.

The bricks remaining in the hearth and the floaters in the slag absorbed a small amount of metal. The properties of these and of samples from the wall are as follows:

	Bricks from Hearth	Floaters	Bricks from Wall
Apparent porosity, %	11·4–14·7	6·5–18·6	14·9–21·4
Average	13·1	10·6	18·9
Bulk density, g./c.c.	1·95–2·07	1·84–1·90	1·53–1·63
Average	2·01	1·88	1·57
Apparent specific gravity (by porosity)	2·26–2·34	1·98–2·25	1·91–1·95
Average	2·31	2·10	1·93

The pick-up of metal by the carbon, probably associated with the high working pressure in the

hearth and the fluidity of the iron, had no serious detrimental effect on the carbon and was shown by resistivity measurements and microscopical examination to be discontinuous, owing to the non-wetting properties of the carbon. When this iron was dissolved with hydrochloric acid, the cold-crushing strength and porosity figures were of the same order as those for the unused bricks.

The question of after-contraction and its relationship to hearth design is one of prime importance. As it was possible to remove complete bricks from the outer rings of carbon many of these were measured and their measurements compared with the data available on the original bricks, as follows :

	Length	Breadth
Hearth brick contraction, %	1·1	1·1
Wall brick contraction, %	Nil	Nil

It appears, therefore, that the degree of contraction experienced by the carbon bricks in the hearth is of the same order as that obtained in the laboratory test on firing for two hours at 1500° C., *viz.*, 1%. The absence of shrinkage in the wall bricks should be noted as clearly demonstrating that the water-cooling on the wall was very efficient.

Fireclay Bricks

All the fireclay bricks examined were heavily impregnated with metal which formed a complete network around the grains, as shown in Fig. 4. Several bricks were measured and it was apparent that, even in the more protected positions of the hearth from which these samples had been obtained, some contraction of the brick had taken place. No statistical data on the original dimensions of the fireclay brick were available, but, even assuming that they were originally $\frac{1}{16}$ in. undersize in length, the measurements made indicated that a linear contraction of the order of 5% in length and slightly greater in breadth had taken place. Considering that these bricks were not exposed to the same conditions as the carbon, the advantages claimed for carbon on a basis of after-contraction seem more than justified.

It was concluded from this examination that carbon as a refractory material is suitable for hearth construction and especially for hearth walls. The expected advantages of higher chemical and mechanical stability and higher thermal conductivity were all realized. No signs of oxidation were evident, nor of any tendency for the metal to break out at the junction of the vertical wall with the hearth. The examination of the wall bricks had shown that, by taking advantage of the high thermal conductivity of carbon, after-contraction can be prevented and hearth walls built quite safely in normal brickwork using small standard shapes. Because, as far as is known, there has been no attempt as yet to cool below the floor of a furnace, after-contraction of the bricks in the floor is likely to take place, and it is therefore imperative to make wall construction independent of floor construction. At Appleby-Frodingham, hearth side-walls are continued right down to foundation level for this reason, thus enclosing the floor in the chimney formed by the walls. The fact that in this first trial, and later in others of the same design, the complete bottom, with the exception of two outer rings, lifted early in the campaign is no condemnation of the carbon brick nor of any change within the carbon brick in service. It is attributed solely to the unsatisfactory design used in building and to not allowing sufficient keying action between the bricks.

The problem therefore still remained of finding some method of holding the carbon of the bottom in place. This could be done by preventing after-contraction, or by holding it in place mechanically.

Up to the present it seems impossible to prevent after-contraction of the bricks in the centre of the hearth, and it was obvious that the only practical solution lay in some interlocking design of brick or block to hold the hearth in position mechanically. After considering many ideas, including the German and American designs, the corrugated block which is at present in use was evolved. A typical set-up of these blocks, as in service in five Appleby-Frodingham furnaces, is shown in Fig. 5. On the last four bottoms installed, these blocks have been set dry using no jointing cement or ramming except in the small make-up areas at the periphery of the hearth. This design thus gives interlock in depth, in addition to the horizontal interlock, and at the same time staggers all joints except the corrugation. This lock is also made sufficiently large to take up the known after-contraction of the bricks across the full diameter of a hearth even if it all took place at one set of corrugations between two blocks. Only two sizes of block are used at present, 10 in. × 10 in. × 20 in., and 10 in. × 10 in. × 15 in., and these allow hearths of various depths to be constructed. Consideration was given to a suitable depth of carbon to be used in the bottom, several factors having to be borne in mind in arriving at a suitable economic figure, *i.e.* :

(1) The greater the depth of carbon the greater will be the mechanical stability of the hearth. With a knowledge of the mechanical properties of the brick it may be calculated that there is a good factor of safety in any tendency for the hearth to split, due to upthrust from molten iron that might penetrate beneath the hearth

(2) It is desirable that the temperature gradient through the bottom should be such that the underlying fireclay bricks do not reach a softening or vitrification temperature. Should this occur, the shrinkage of the fireclay brick would obviously imperil the stability of the carbon. It was also desirable that the temperature at the bottom of the carbon should be low enough to freeze the iron which will undoubtedly penetrate into the joints.

It was finally suggested that a depth of carbon of 5 ft. would be sufficient. The general arrangement of the first such bottom, in a 22-ft. diameter furnace at Appleby-Frodingham, is shown in Fig. 8. No furnace with a corrugated block bottom has yet been blown-out, and it is not yet known whether salamander formation has been prevented.

Work is in hand on the solubility of carbon in molten iron. It is too early to make any definite statement, but it would appear that certain types of carbon do dissolve appreciably in iron whilst others are much more resistant. Whether the solution rates

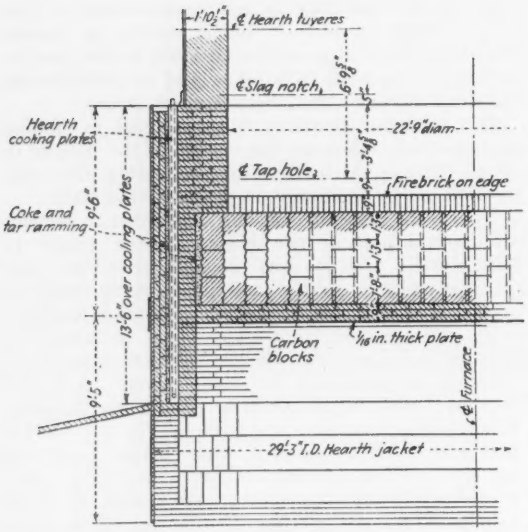

Fig. 8—Furnace bottom, showing interlocking carbon-brick construction

are significant under blast-furnace conditions must await the examination of hearths at present in service.

The Taphole

Some users of carbon hearths, especially in the United States, have experienced considerable trouble with furnaces in which they have incorporated a carbon taphole. It was decided against their use at Appleby-Frodingham. The danger of air getting to the carbon and giving the oxidizing conditions necessary for the burning or ' perishing ' of the carbon is greatest at the taphole, especially during casting time and when blowing on the hole. In addition, all stopping mixes contain moisture which at the temperatures prevailing in the hole is just as efficient in destroying carbon as is air. A properly maintained taphole is being continually rebuilt after each cast, and no serious trouble should be experienced with normal materials. All tapholes, therefore, are made in 42% alumina fireclay brick, 13½ in. being considered a satisfactory thickness to separate the carbon of the main lining from the hole. Carbon is used round the slag notch at Appleby-Frodingham.

CARBON IN THE BOSH

Early in 1945, the possibility of extending the use of carbon throughout the furnace was discussed and tentative programmes were prepared for using carbon in positions higher in the lining. The first and obvious place to try was the bosh, especially as some of the older Appleby-Frodingham furnaces employed spray-cooled boshes with only about 14 in. of fireclay brick. No serious troubles have been encountered with this construction, which has much to commend it from the point of view of cost and of fewer sources of water leakage into the furnace.

On blowing-out a bosh, it has usually been found that little recognizable brickwork remained. The improvements in carbon which had been made and the known non-wetting properties of the bricks, which it was thought would greatly assist the maintenance of a clean bosh, gave further encouragement to this extension of the use of carbon.

Accordingly, in 1945, two 17-ft. furnaces were relined using carbon from floor to lintel, except for the tuyere belt which was still in fireclay brick. The boshes were built with a single 13½-in. brick thickness enclosed in a welded steel jacket and spray-cooled. One of these furnaces was blown-out in October, 1949, and the other in March, 1950. Almost perfect behaviour of the refractory in service over four years was shown in each furnace. The bricks went in 13½ in. long and came out 13–13½ in. long. Two changes were noted, in that the working face of the bricks had smoothed off to the bosh angle and that there was some absorption of what appeared to be alkali salts. A photograph of this bosh, taken during the wrecking of No. 1 furnace in October, 1949, is shown in Fig. 6.

Since 1945, other furnaces have been put in with carbon boshes, and experience on four during the last four years indicates that a carbon bosh keeps cleaner than a fireclay bosh, two of the main reasons being the higher abrasion resistance and the known non-wetting properties. Carbon hearths and boshes have become standard practice at Appleby-Frodingham.

CARBON IN THE STACK

Several of the scaffolds which have occurred at Appleby-Frodingham in recent years on stacks lined with what is accepted as a good fireclay brick have been examined in detail and reported.[2] Without going into theories which have been evolved as a result of these examinations and of many laboratory experiments, it is considered that the main factors leading to scaffold formation in Appleby-Frodingham practice appear to be :

(1) Sticking of the burden to the fireclay inwall

(2) Carbon deposition tending to push the working face of the lining into the furnace

(3) Alkali attack on the fireclay brick cementing the scaffolding zone together and forming very refractory potash alumino-silicate compounds such as kalsilite and kaliophilite.

All these effects have one thing in common: the fireclay material of the stack. It was therefore in the refractory that a solution to the scaffolding problem was sought. Laboratory work was beginning to show that carbon was an obvious choice, if not to do the job, at least for a trial. The well-known non-wetting properties of carbon again indicated this, since in ' stickability ' tests, in which samples of ore were sintered and melted on various refractories, carbon was the only one which was not attacked and from which the melted ore could be detached, like an ingot from the mould, on cooling. Laboratory work also showed that in the normal carbon monoxide deposition tests no disintegration was obtained on the commercial brands of carbon brick available. Finally, alkali tests were carried out and, unlike fireclay, there was no attack nor formation of compounds which assist scaffold build-up.

One difficulty was the question whether carbon bricks would burn in the carbon dioxide present in the gas passing up the stack. Laboratory work

showed that carbon bricks do burn rapidly in carbon dioxide at temperatures of 1000° C. and above (the reaction actually starts at about 700° C.) but that this burning decreases considerably in the presence of carbon monoxide and nitrogen, the first acting as an inhibitor in the reaction $C + CO_2 \rightleftharpoons 2CO$, and the second as a diluent. It was also known from gas surveys in furnaces that at temperatures of 1000° C. the usual concentration of carbon dioxide at the inwall is negligible. Low in the stack where the temperature is sufficiently high for burning to take place the gases contain CO but virtually no CO_2, whereas high in the stack where there is CO_2 the temperature is normally too low for burning. The risk of burning in the stack, therefore, would be very small, especially as before any carbon dioxide reached the carbon lining it would have passed over coke at a slightly higher temperature than the inwall and should have reached equilibrium with the carbon and carbon monoxide.

At this stage in the evolution of the all-carbon furnace, consideration was given to the question of the relative abrasion resistance of carbon and fireclay bricks. It has already been mentioned (*see* p. 274) that some of the commercial brands of carbon brick available have resistances to abrasion approximately twice those of fireclay bricks used for stack linings.

With the weight of evidence in favour of a trial of carbon bricks in a stack, it was agreed to insert several bricks in stack cooler holes. Examination of these after a few weeks showed that no burning had taken place, and a panel of about 50 bricks was built half-way up the stack of No. 5 furnace. Examination during operation again showed no burning, so that in December, 1946, a complete 15-ft. belt of carbon was built into the stack of the 22-ft. dia. No. 10 furnace. The mid-point of this belt was about 40 ft. above the tuyeres and was, therefore, in the zone where scaffolding was likely to take place. This was a preliminary trial, and therefore the carbon belt was backed up with normal fireclay brick ; a diagram of the construction is shown in Fig. 9. The arguments used against this practice in hearth construction do not apply in stack construction. Periodic examinations were made of this belt and showed that for over two years it was completely intact, with no sign of scaffold or scab formation or of wear. Check trials in another furnace showed similar results.

Latterly, it was found that some of the carbon bricks of the original belt had gone. After a few further months of operation, test drillings showed that the carbon belt was actually standing proud of the fireclay bricks above and below, and that they were being pushed into the furnace by carbon deposition in the backing fireclay. The carbon panel was thus lost, but after three years in the stack showed negligible wear, burning, or scaffold formation.

THE FIRST ALL-CARBON FURNACE

It was decided that No. 6 furnace, due for relining in the spring of 1949, would be built with no fireclay bricks below the throat armour except around the taphole, thus integrating in one all-carbon furnace the ideas and experience of the previous seven years.

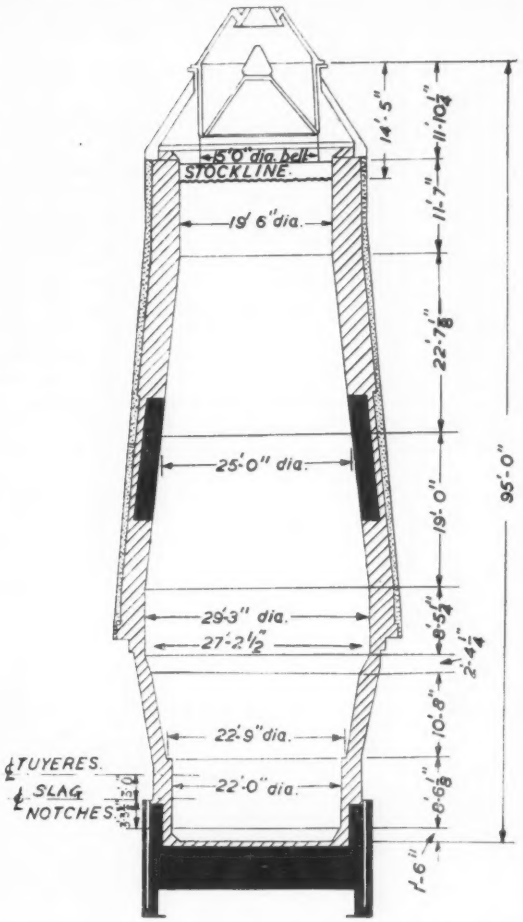

Fig. 9—Section of furnace, showing carbon belt built into stack

A diagrammatic sketch of No. 6 furnace is shown in Fig. 10.

The use of only 13½ in. of carbon in the bosh had been so successful in trials over the previous three years that it was decided to use only 9 in. in the bosh of No. 6 furnace, again spray-cooled. This, together with a little alteration of the lines, permitted some enlargement of the furnace within the existing casing, and if a 9-in. bosh were successful in an 18 ft. 3 in. furnace there would be no hesitation in using, say, 18 in. of carbon spray-cooled in a larger furnace. This possibility of using less refractory in the bosh and spray-cooling is an important one, leading to big savings in initial cost, in copper consumption, and in cooling-water costs. The hearth of the furnace was built with corrugated blocks, as shown in Fig. 5, and standard shapes were used in the hearth wall and tuyere belt. The hearth was spray-cooled, the tuyere belt was sparingly cooled with copper plates, and the bosh was spray-cooled. This tuyere belt was not plated, the cold end of the bricks being exposed to

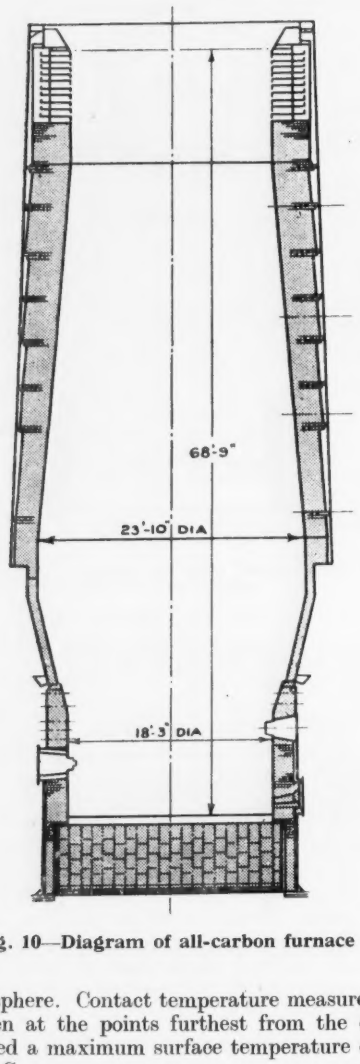

Fig. 10—Diagram of all-carbon furnace

the atmosphere. Contact temperature measurements were taken at the points furthest from the coolers and showed a maximum surface temperature of just over 100° C.

It was considered that with the favourable properties of carbon there was no need to use the normal stack coolers. It was calculated that without insulation, at the lower levels, shell temperatures of about 200° C. would be obtained, so that some insulating material was essential. It was also necessary to have some means of cushioning the expansion of the lining against the shell. A mixture of coke nuts and carbon ramming was used; when the stack became hot, however, the tarry component of the ramming started to run down through the packing, concentrated in the lower stack, and for a time destroyed the insulating value of the coke. A few days after blowing-in, the temperature of the shell just above the lintel was 150° C. On this section, therefore, spray-cooling was used, and no difficulty has been experienced. It is possible that most of the trouble could have been avoided by using dry coke nuts with no tarry ramming.

Furnace Operation

No preliminary drying of the furnace was carried out, all bricks having been protected from the weather in transit and on site. Four rows of thermocouples were put in the stack and coupled to a recorder before blowing-in. The first few days' record of a vertical row of four is shown in Fig. 11 and illustrates how rapidly the stack refractories were brought up to working temperature after putting on wind. These thermocouples were 2½ in. behind the working face, and after operating for a few weeks they were withdrawn for examination. The front 2½ in. was found to be still in position, so that the rapid heating apparently had done no damage.

After five months of operation the furnace was taken off wind for a few hours to allow an examination of the stack brickwork to be made. Temperature-gradient determinations more or less confirmed impressions of what had been happening to the coke and tar ramming. They showed that the coke breeze was giving insulation to the extent that a 6-in. thickness was bringing the brick/coke interface temperature of 600° C. down to a shell temperature of 150° C. Calculations from known thermal conductivity data showed that other insulating materials would give lower shell temperatures and more efficient insulation, and it was agreed to use these in the next furnace to be made all-carbon. In this exploration of the stack it was found that in places the 2½-in. piece in front of some of the thermocouple holes had been dislodged, owing to mechanical instability. That this was not a general loss of 2½ in. over the stack was checked by drilling in various positions remote from the thermocouple holes. Regular shell temperature measurements were made with a contact pyrometer at times when the spray water on the lower stack was turned off, and there was no difference between the readings taken after one year and those taken after one month. At the points drilled the working face of the bricks was absolutely clean and there was no sign of scab or scaffold.

In carrying out explorations of the stack it was noted that drilling through hot carbon at about 800° C.

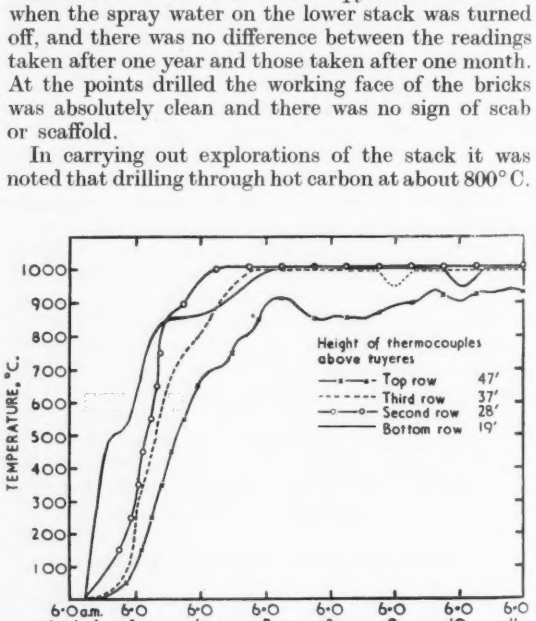

Fig. 11—Chart of stack thermocouple readings during blowing-in of No. 6 all-carbon furnace

took the same time as drilling through the same thickness of carbon in the cold with the same equipment and operators. This can be taken as an indication that carbon does not lose its toughness and resistance to abrasion at stack operating temperatures.

From the production viewpoint, No. 6 furnace, since blowing-in on the 4th April, 1949, has been entirely satisfactory. Although the furnace had not been dried out, the first cast was most successful, and, 23 hours after putting on wind, iron came using about 8 ft. of oxygen lance. In the first year of operation, the furnace made 12% more iron at a slightly lower coke rate than it did in the equivalent period of its previous campaign. It may be thought that this was due to the change in the lines, but this furnace was only very slightly larger than that in the previous campaign, and it must be remembered that even this was possible only because carbon was used. There has never been any suspicion of trouble in the hearth, and there is no evidence of scab or scaffold in the stack.

Trouble has, however, been experienced more recently in overheating at the top of the stack of No. 6 furnace. This was originally associated with the armouring-bar region, which was lined with fireclay bricks, but has now extended down into the carbon. Trouble with fireclay in the armouring bars is by no means new, but there is the possibility that in this particular furnace it may have been aggravated by failure of the carbon below. No definite explanation can be given, however, until the furnace is opened out. The only other cause for suspicion with carbon stack linings has been the disappearance, after nine weeks, of a radioactive tracer pellet 6 in. behind the working face just below the armouring in No. 5 furnace. This, however, cannot represent the average rate of carbon loss in the stack, as otherwise very serious trouble would long since have been experienced with No. 5 furnace. These observations emphasize the essentially experimental character of the use of carbon bricks in stack linings.

The question of heat losses through carbon has, of course, been examined. It was felt, and experience confirms, that with regard to the hearth the heat losses of carbon and fireclay linings show little difference. The lower thermal conductivity of the fireclay brick is normally offset by the fact that after being in service for only a short time a much smaller thickness remains and the conductivity of the brick is greatly increased by the absorption of metallic iron. Actually, a little less water is needed in spray-cooling the carbon hearth of No. 6 furnace than when fireclay was used. Calculations on the heat losses through the stack raise doubts as to the efficiency of the coke insulation at present in use. The heat losses, however, are small and to all intents and purposes insignificant. The fact that shell temperatures of the order of 150° C. are obtained is not dangerous, although such temperatures are uncomfortable, but the aim is to obtain temperatures less than 100° C. Experimental investigations have shown that exfoliated vermiculite would give satisfactory results in obtaining this, and at the same time would maintain a safe interface temperature for the insulation. It was, therefore, decided that this material would be used in the second all-carbon blast-furnace.

THE SECOND AND THIRD ALL-CARBON FURNACES

No. 1 furnace, Frodingham, which is the same size as No. 6, became the second all-carbon furnace. This furnace had been relined in 1945 with a carbon hearth and bosh and a fireclay stack. No trouble had been experienced with the hearth, which was of the inverted dome type, or with the bosh, and on examination after blowing-out in September, 1949, it was found that the centre of the bottom had floated as before, but the hearth walls and bosh were in excellent condition. The stack had given much trouble due to scaffolding and failure of the top brickwork. There was, however, one interesting feature. A small panel of carbon bricks had been installed at *E* row cigar cooler level when the furnace was built. These bricks were salvaged from the stack in excellent condition although the surrounding fireclay brickwork was disrupted almost beyond recognition.

The same type of spray-cooled corrugated hearth and 9-in. bosh as used in No. 6 furnace, was installed in this furnace. It was decided to instal in the stack the normal cooling arrangements in addition to the vermiculite insulation previously agreed. This would give an almost normal stack with carbon bricks instead of fireclay. The added and important advantage was that as the life proceeded, and if experience indicated the desirability of such a step, cigar coolers

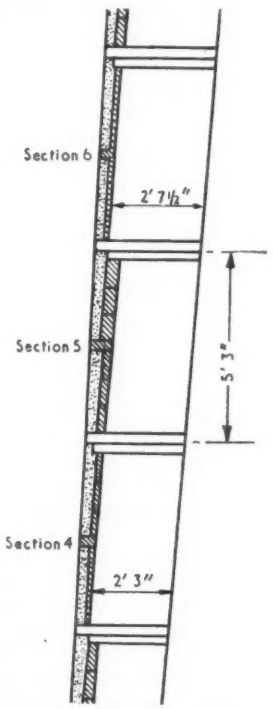

Fig. 12—Diagram showing the insulation construction used in the stack of No. 1 furnace. (The second all-carbon furnace)

could be drawn and rammed up with carbon, eventually leaving an all-carbon stack completely insulated with a first-class vermiculite insulator. A diagrammatic sketch showing the insulation behind the lining (without the coolers) is given in Fig. 12. This insulation consisted of vermiculite loose fill and vermiculite brick, the latter being used to prevent excess 'denaturing' (*i.e.*, loss of the combined water) of the loose fill and, by bonding through to the shell as shown, to prevent settling of the loose fill to the bottom of the stack. For ease of building, and following the lines of the furnace, the thickness of carbon varied from 1 ft. 10½ in. in sections 1, 2, and 3, to 3 ft. in section 8, each section being 5 ft. 3 in. high. The carbon bricks were bonded through to the shell between each section. The minimum insulation allowed was 1½ in. of brickwork and 3 in. of loose fill, and the maximum, 4½ in. of brickwork and 4½ in. of loose fill.

No. 1 furnace was blown-in on 24th October, 1949, and has operated satisfactorily now for 14 months. In the first year of operation 4% more iron was made than in the first year of the previous campaign. At the beginning of November, 1949, the heat lost to the 140 coolers in the stack (10 rows, 14 per row) was measured and was found to be very small, equivalent to the heat input of only 1·3 tons of coke per day, or 11·4 lb. of coke per ton of iron.

At the end of February, 1950, No. 5 furnace, Appleby-Frodingham, was blown-out and relined with carbon in the same way as No. 1 furnace. This furnace was blown-in at the beginning of April, 1950, so that there are now three all-carbon furnaces operating at Appleby-Frodingham.

ECONOMICS OF THE ALL-CARBON BLAST-FURNACE

It has been said that carbon bricks are rather expensive to use for blast-furnace linings; however, as has already been mentioned, in the first year of operation of No. 6 furnace, 12% more iron has been made than in the first year of the previous campaign with a fireclay lining. This alone paid for the extra cost of the carbon installed. The extra cost, even if the same campaign life were obtained, would be negligible per ton of iron produced. A longer campaign than normal is, however, hoped for, and certainly one much freer from breakouts and scaffolding, and if scaffolding is eliminated the cost of the carbon will be more than offset.

The cost of carbon below the tuyeres can be ignored because the use of carbon in the hearth is now standard practice and is justified by the reduction in the incidence of breakouts. Carbon in the bosh pays, for the reasons already mentioned, and will certainly be economical in the larger furnaces where a thinner lining, and reduced costs of cooler construction will be possible. The extra cost of building in carbon in No. 6 furnace was of the order of £8000.

It may be slightly misleading to talk of financial sums, since some of the modifications and benefits which only carbon can make possible should also be considered. It is felt that certain benefits accrue from the use of carbon, and can be set against the increased cost. These benefits are as follows:

(1) Because of better refractoriness and abrasion resistance it is safe to use a thinner lining with carbon than is normally employed with fireclay brick, so that a bigger furnace in an existing casing may be used to make more iron or the same amount of iron with a higher efficiency. With a new furnace a smaller shell is required for a given output.

(2) Carbon should increase the overall campaign life of the furnace.

(3) The furnace construction can be greatly simplified. This has been demonstrated by the successful use of boshes with a very thin lining and spray-cooling.

(4) While it is early yet to say, it is thought that stack cooling may eventually prove unnecessary.

(5) A carbon furnace should maintain cleaner lines than a fireclay furnace.

(6) Hearth breakouts should be reduced to the absolute minimum. This has already been shown.

Any one of these factors is sufficient to offset the extra cost of carbon, and the wider use of carbon in blast-furnace plants will undoubtedly bring down the cost of the refractory.

CONCLUSIONS

The evolution of the all-carbon blast-furnace, far from being complete, is still very much in the experimental stage. It does appear, however, from all outward signs that there will be almost complete freedom from breakouts and scaffolds, and it should be remembered that it is for these two reasons only that carbon was introduced. All other benefits are incidental, but as they have come to light they have proved to be so interesting, and such lucrative lines to follow, that the main issue tends occasionally to be forgotten.

Acknowledgments

The development and use of carbon in blast-furnace linings is the result of the close co-operation of a large team of people. Mr. G. W. Wells, General Manager of the Appleby-Frodingham Steel Company, and Mr. F. H. Saniter, Director of Research of The United Steel Companies, Ltd., have throughout given every encouragement and support. The operating staff of the Appleby-Frodingham Blast-Furnace Department have worked hard and have contributed much to the successful application of carbon in the blast-furnace. Carblox Ltd. have, from the beginning, worked very closely with the Research and Blast-Furnace Departments, and have been more than helpful in adapting the manufacture of their products to suit the needs of the furnace.

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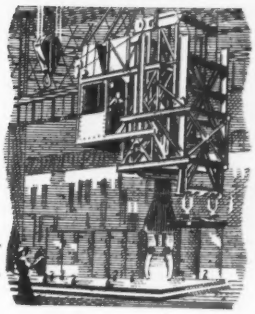
B. I. S. R. A.

BRITISH IRON AND STEEL RESEARCH ASSOCIATION



Ingot Heat Conservation

INGOT MOULD TEMPERATURE MEASUREMENTS

By R. T. Fowler, B.Sc. Ph.D., and J. Stringer, B.A.

SYNOPSIS

Further measurements on the surface temperatures of an ingot mould have been made, and the technique of temperature measurement by fixed thermocouples has been extended to include temperatures through the walls.

Two trials were made with the mould initially at a temperature of 30° C., and one trial with the mould at 125° C., the latter being representative of normal practice. Temperature gradients and isotherms have been plotted to give a picture of the overall temperature distribution throughout the mould at various times from teeming.

From the results obtained, the heat content of the mould was calculated and the amount of heat removed from the steel during solidification was estimated.

Introduction

A RECENT report on casting-bay practice[1] indicated that the thermal history of ingots was likely to be different both between casts and between ingots within one cast. It was considered that a knowledge of the temperature distribution and heat content of the ingots at all stages between teeming and rolling would be required, to show whether the empirical factors of safety allowed could be safely reduced, thereby reducing the overall fuel requirements of the soaking pits.

Because of difficulties in the direct measurement of the internal temperatures of an ingot, it was felt that in the first instance surface temperatures only should be measured, and that these measurements should be used to calculate the internal temperatures.

The first experiments were carried out by Brancker,[2] who used thermocouples fixed to 36 positions just below the surface of a mould in a casting pit, and showed that such a technique was practicable without interference to normal operation. Considerable differences in temperature were found to exist between the various mould positions, and it was felt necessary to measure the temperatures through the mould wall. Besides giving a direct indication of the rate of heat absorption by the mould, such measurements would give a check on calculated internal mould temperatures.

The present paper describes the technique adopted and the results obtained during one run taking surface temperatures only, and three runs taking internal temperatures at various positions on the centre plane of the mould.

EXPERIMENTAL TECHNIQUE

All trials were made on an 8-ton slabbing ingot mould filled with rimming steel cast at temperatures of 1530–1550° C. Trials 1 and 2[2] had been made using a millivoltmeter. Trial 3 was identical with trials 1 and 2, except that automatic recording with a high-speed potentiometer was used and the trial was carried out with thermocouple plugs placed around the mould in three planes of 12 couples (three to each face). The results obtained gave the temperature of the mould $\frac{1}{2}$ in. below the outer surface.

Figures 1 and 2 show the arrangement used for measuring the temperature gradients through the mould wall in trials 4–6. Forty-four chromel–alumel (22 S.W.G.) thermocouples were used; 8 couples were fixed $5\frac{1}{2}$ in. into the wall, 8 couples at 4 in., 12 couples at $1\frac{1}{2}$ in., and 16 couples at $\frac{1}{2}$ in., as close as possible to the centre plane of the mould.

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Dr. Fowler is in the Plant Engineering Division of the British Iron and Steel Research Association. Mr. Stringer is with London Transport Executive, and was formerly in the Plant Engineering Division of the Association.

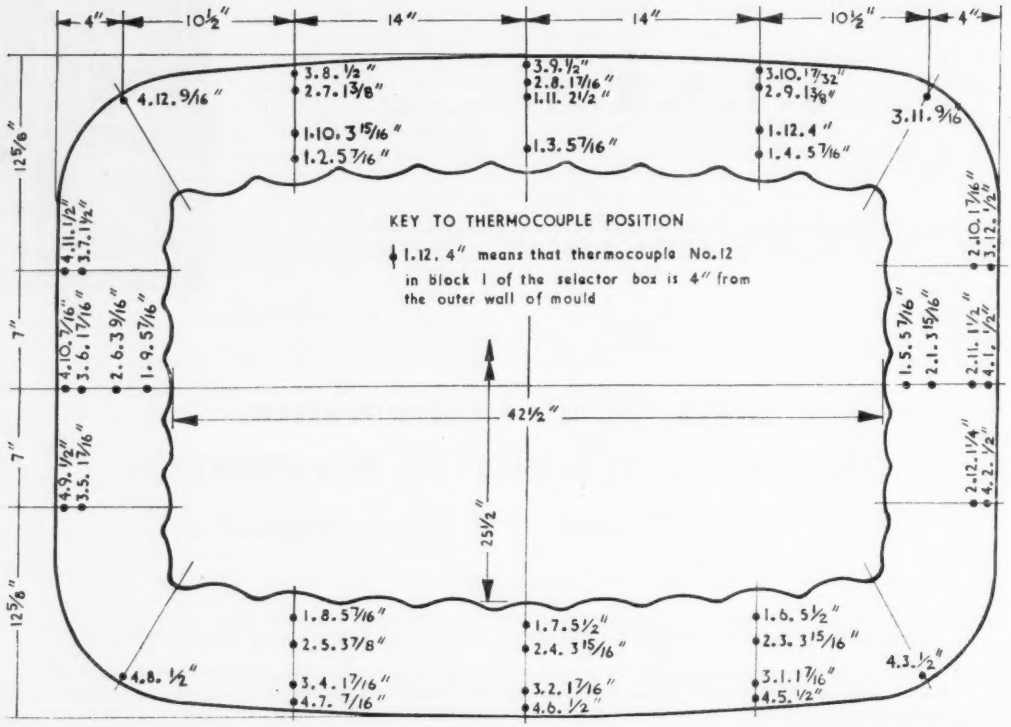


Fig. 1—Thermocouple positions in centre plane of mould. Weight of mould 8·8 tons, weight of ingot 8·0 tons

The deep-seated couples were made up in specially machined mild-steel sheaths consisting of three parts (*see* Fig. 2). The wires were inserted through the plug, from which they were insulated by a coating of silicone varnish. They were then twisted, and the junction was hammered into the small slot at the end. Hollow spacers were used to position the plugs and to ensure that the junction was tightly pressed against the bottom of the hole. The wires were insulated from the spacer and plug walls by twin-holed refractory insulators. The assemblies were inserted into the holes, and were secured in position by means of the small drilled screw plugs.

In the surface thermocouples, the screw plugs were drilled, and the wires were insulated from the plug by means of small fish-spine insulators. Outside the mould the chromel and alumel wires were covered with silicone-impregnated glass-fibre sleeving. This sleeving was used because of its resistance to fairly high temperatures; it was found to be in perfect condition after the experiments.

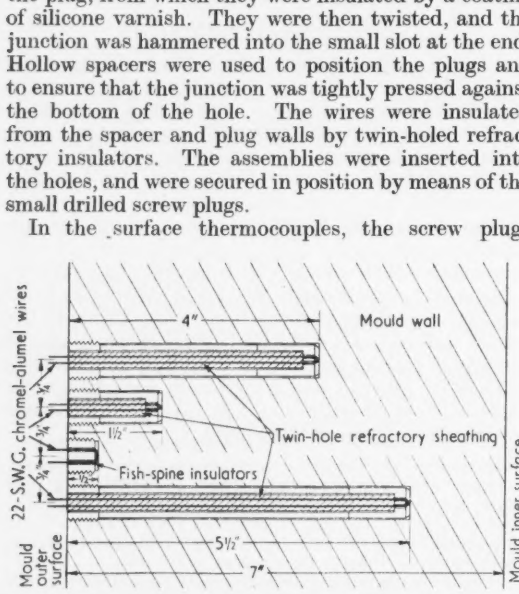


Fig. 2—Diagram of thermocouple plugs in position in mould wall

The wires were connected to two Sindanyo terminal boards fixed 9 in. below the top of the mould and 6 in. from the surface. The 44 positive wires were connected individually to brass terminals on the boards, and the negative wires were connected equally to 12 terminals. Thus each board carried 22 positive terminals and 6 'common' terminals. These 'common' terminals were interconnected by an alumel wire (22 S.W.G.) which was joined at one end of each board to a 'common' constantan wire leading to the recorder box. The positive terminals were connected to copper wires (22 S.W.G.) in glass-fibre sleeving.

The 23 wires from each board were bound with asbestos tape, and 3 ft. from the mould the two sets were formed into one combined cable, 80 ft. long × 1¼ in. diameter. This cable was draped along the pit wall clear of the ladle carriage, and led to a 48-point selector box[3] of the recorder, which was placed in a cool place in the shop.

The combined resistance of the cable, the couples, and the selector box, was 24 ohms; this resistance is too high for the satisfactory use of millivoltmeters as a recording medium, but is well within the range of use of a potentiometer.

The selector box was connected to a high-speed recording potentiometer, which had a 12-point print wheel and printed when it reached a balance. The whole cycle of recording the 44 temperatures indicated by the couples was completed in about 1 minute. The time difference for temperatures close to one another was approximately 1 second, and for a full-scale traverse was about 3 seconds. The selector box had 48 positive terminals, arranged in four blocks of 12, and one 'common' terminal for the negatives. In each block of 12, one couple was short-circuited to act as a marker and to assist identification. The couples were connected to the box so that all the $5\frac{1}{2}$-in. deep couples were recorded in succession, followed by the 4-in., the $1\frac{1}{2}$-in., and finally the $\frac{1}{2}$-in. couples. This arrangement ensured that the distance the pointer of the recorder had to move between readings was a minimum, thereby reducing the time of each cycle. The scheme of couple numbering and identification is shown in Fig. 1.

The complete mould assembly was placed in the casting pit in the positions shown in Fig. 3. To protect the couple wires from metal splashes and possible damage, the mould was plated with steel sheets around the edges until the rimming action of the ingot had subsided.

The recorder was started at the moment the metal hit the bottom plate, and was allowed to run until the ingot was stripped.

Procedure of the Trials

Trials 1 *and* 2—The procedure of these two trials has been described by Brancker[2] in a previous paper. Subsequent trials in the present investigation were a continuation of his work, with the measurement of temperatures extended through the mould wall.

Trial 3—This trial was identical in procedure with trials 1 and 2.[2] The test mould was set in position 1, and moulds 3 and 4 were not filled. Asbestos-rubber-covered compensating leads were used from the terminal boards, but owing to the burning of the rubber in the cable and the possibility of spurious readings, it was decided that in future trials its use would be abandoned, and that glass-fibre sleeving on copper wire would be used instead.

Trial 4—The mould was placed in position 14 in the pit, and only mould 13 was filled, moulds 11 and 12 being left empty. This trial utilized the technique in which glass-fibre-sleeved wires were used. Two copper–constantan couples were used to measure the temperature of the terminal boards on the mould, one couple being in the middle of the board and the other at one end. A chromel–alumel couple was fixed in the pit wall and was shielded from direct radiation from the mould. These temperatures were measured periodically throughout the trial by means of a millivoltmeter. The recorder was allowed to run for 2 hr., until the ingot was stripped.

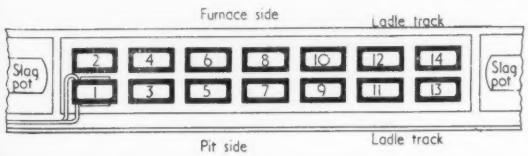
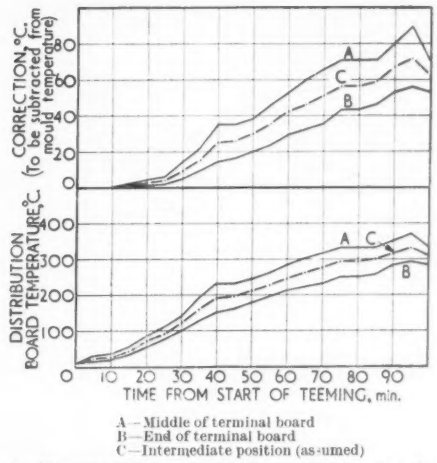


Fig. 3—Plan of casting pit, showing mould positions



A—Middle of terminal board
B—End of terminal board
C—Intermediate position (assumed)

Fig. 4—Temperature correction due to heating o terminal board

After the mould had been removed from the pit and set on the bank to cool, the recorder was started again and was allowed to run intermittently for 6 hr. to obtain a cooling curve for the mould.

Trial 5—After the completion of trial 4 it was found that two couples (Nos. 2.12 and 3.5) had broken and were impossible to replace. It was decided to utilize the two vacant board terminals and to use copper–constantan couples to measure the temperature of the boards. This method removed the need for the millivoltmeter. The mould was set in position 13 in the pit, and moulds 11, 12, and 14 were filled. The trial was stopped when the ingot was stripped, after 1 hr. 50 min. from teeming.

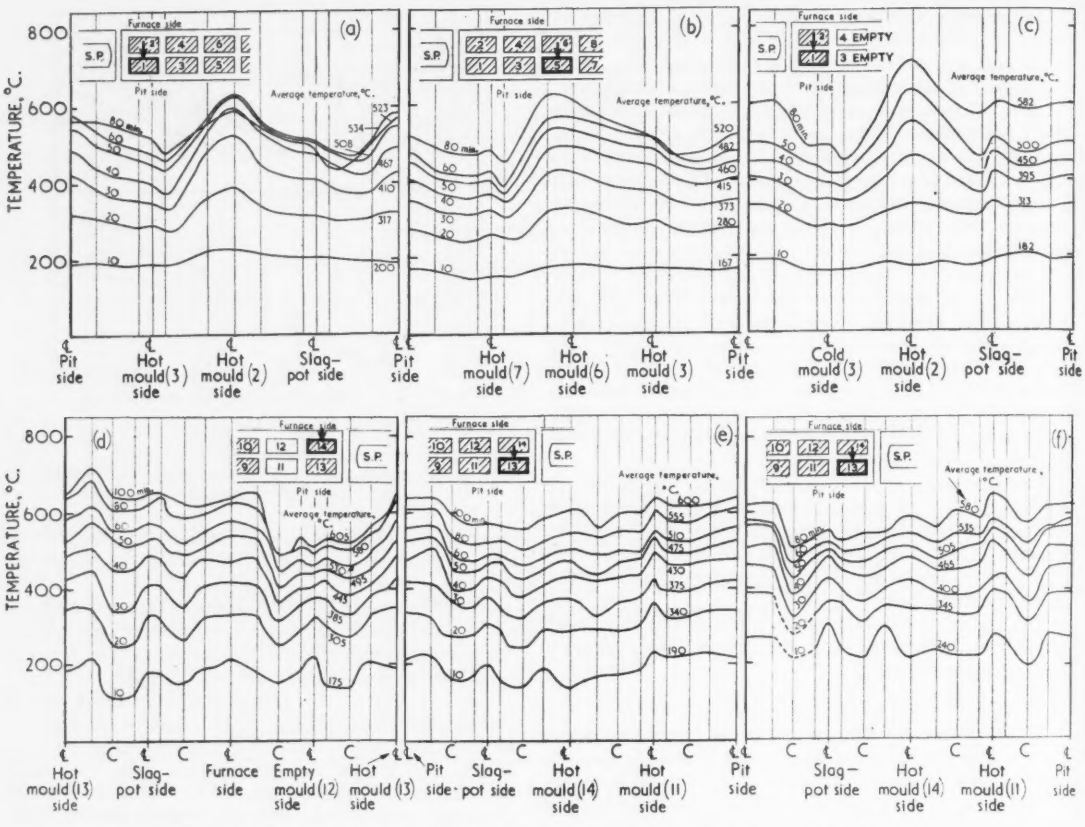
Trial 6—This trial was identical in procedure with trial 5, but the mould was replaced in the pit after being allowed to cool for 10 hr. on the bank, after trial 5, as in normal practice. (The average temperature of the mould was 125° C.) It was considered desirable to see what effect this had on the mould temperatures and cooling capacity. The ingot was stripped after 1 hr. 30 min.

EXPERIMENTAL RESULTS

All results were corrected for compensating junction temperatures, using the appropriate correction factors for the terminal-board temperatures. Typical graphs and correction curves are shown in Fig. 4. The corrected temperatures should be accurate to $\pm 5°$ C.

Results of Trials

Trials 1 *and* 2—The results obtained by Brancker[2] in trials 1 and 2 have been modified by the corrections in Fig. 4 for cold-junction temperatures on the mould terminal board, and have been replotted in Figs. 5*a* and *b*. These developed-temperature diagrams are plotted to give views in the direction of the arrows in the small key diagrams in each figure. The full moulds are shaded. Corner positions are marked by letter *C* in trials 4–6; there were no



(*a*) Trial 1 (centre plane) (*b*) Trial 2 (centre plane) (*c*) Trial 3 (centre plane) (*d*) Trial 4 (*e*) Trial 5 (*f*) Trial 6

Fig. 5—Developed surface-temperature contours

measurements of corner temperatures in trials 1–3.

Trial No. 3—Measurements were made of surface temperature only, and the developed-temperature diagram is shown in Fig. 5*c*.

Trials 4, 5, *and* 6—The time–temperature curves for the 44 couples were plotted, and a specimen set is shown in Fig. 6. Using the data obtained from the curves it was possible to draw the temperature gradients through the mould walls (*see* Fig. 7). These curves were then used to determine the temperature contours through the mould at fixed time intervals from teeming. The set of diagrams obtained for trials 4, 5, and 6 are shown in Figs. 8, 9, and 10 respectively, and the developed surface-temperature diagrams for these three trials are shown in Figs. 5*d*, *e*, and *f*.

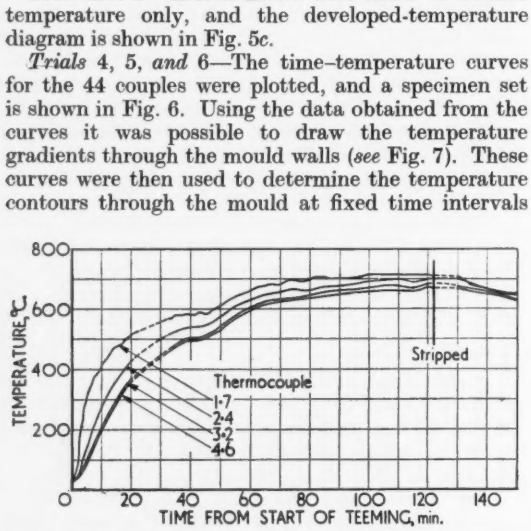


Fig. 6—Time–temperature curves for one set of four thermocouples (trial 4)

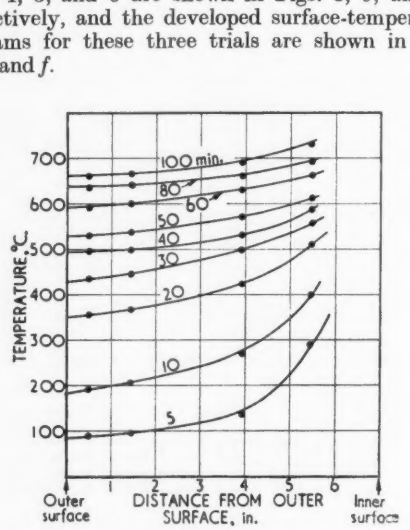


Fig. 7—Temperature gradients through the mould plotted from data in Fig. 6

Time–Temperature Curves of Stripped Mould and Pit Wall

The cooling curves for two couples of a set of four are shown in Fig. 11, for the mould set on the bank. The curves for each couple are nearly identical.

Figure 12 shows the heating-up curve for the casting-pit walls. The temperature of the pit rose to about 300° C. at the time of stripping. When a cold mould is placed in the pit, heating of its outer surface may occur; therefore in the first stages of cooling, reverse temperature gradients may exist in the mould wall.

Heat-Content Calculations

It is possible to calculate, from the temperature contour diagrams, the heat content of the mould at given times from teeming. The areas between successive contours were measured with a planimeter, and the heat contents were calculated for each mass of metal represented by the measured areas. The sum of these heat contents gave the overall heat content of the centre slice of the mould. Knowing the heat content of the mould, it is possible to determine the heat gained by the mould from the steel, in cal./g. of steel.

The areas under the time–temperature contours in Figs. 5*a–f* were measured, and the average surface temperatures were calculated. The approximate cumulative heat losses by radiation and convection were then calculated for trials 4–6. The total heat lost from the steel is the sum of the total heat gained by the mould and the heat lost by radiation and convection.

Figure 13 shows the relationship between time from start of teeming and the heat gained by the mould, and Fig. 14 shows the total heat, including radiation, etc., lost by the steel, *versus* time. The loss by radiation and convection is small, amounting to about 10% of the heat gained by the mould at the time of stripping.

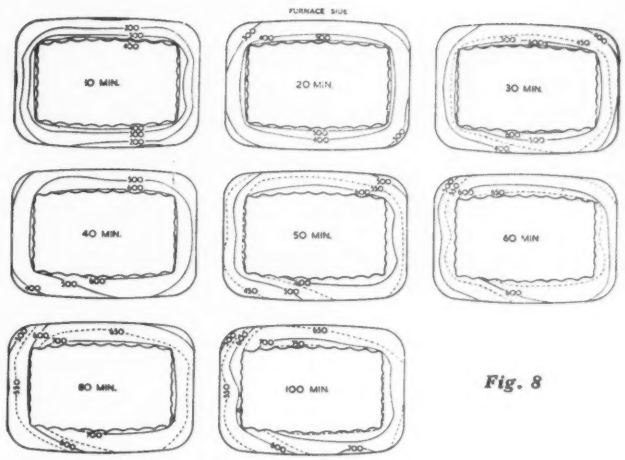


Fig. 8

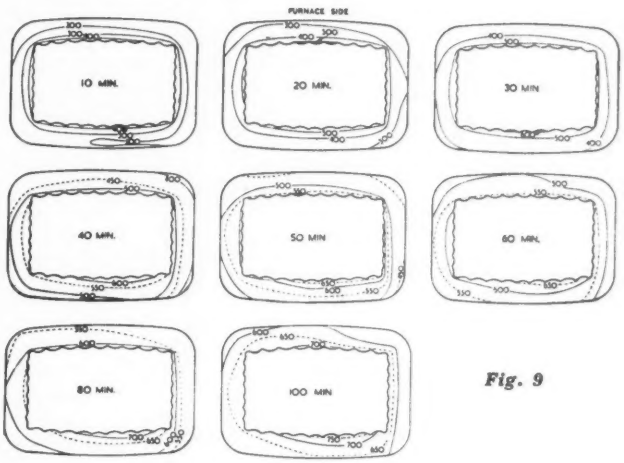


Fig. 9

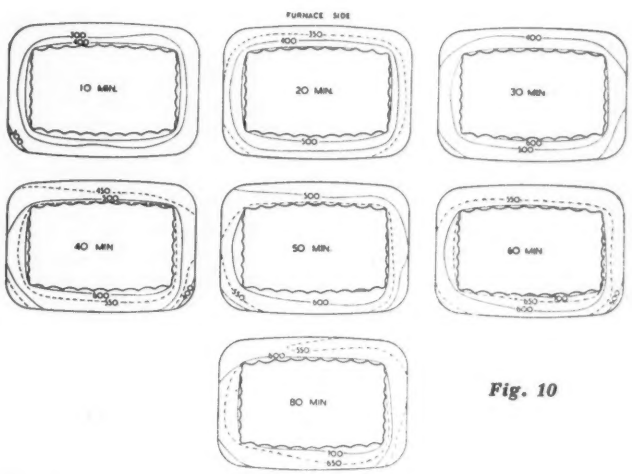


Fig. 10

Fig. 8—Trial 4, mould in position 14

Fig. 9—Trial 5, mould in position 13

Fig. 10—Trial 6, mould in position 13

Figs. 8–10—Mould isotherms at fixed time intervals

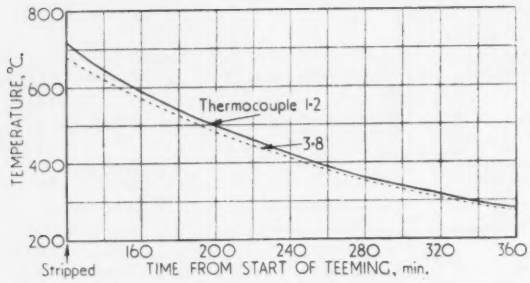


Fig. 11—Time–temperature cooling curves of mould after stripping (trial 4)

$\pm 5°$ C., or to heating of the walls of the mould from other warm moulds or from the pit walls.

(3) Temperature distribution throughout the mould is far from uniform, and the temperature gradients set up probably cause severe stresses across the corners and in the walls (Figs. 8–10).

Acknowledgments

The temperature measurements were obtained at Messrs. John Summers and Sons, Ltd., Hawarden Bridge Steelworks, Chester, and grateful acknowledgments are made to the management and staff for their very valuable help. The authors also thank

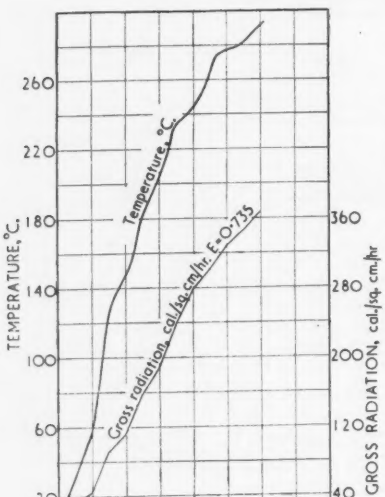


Fig. 12—Time–temperature curve of casting-pit wall

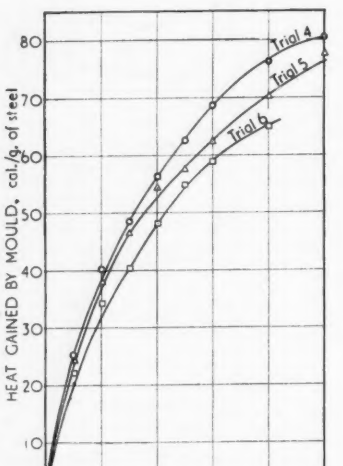


Fig. 13—Heat gained by mould per gram of steel, *versus* time

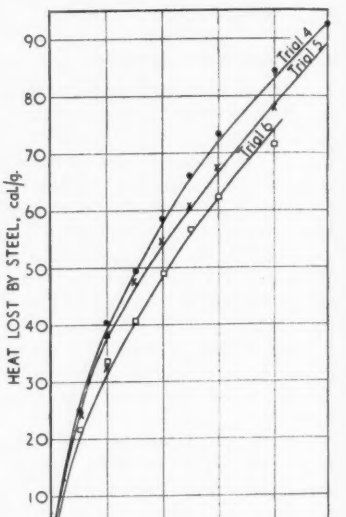


Fig. 14—Total heat lost by steel, *versus* time

CONCLUSIONS

(1) Examination of the developed time–temperature figures shows that considerable differences in temperature exist on the mould faces, depending on the position of the mould in the casting pit (Fig. 5).

(2) After 30 minutes the temperature gradients through the mould wall are shallow, and in some instances are linear. In several cases slight reverse gradients occurred; these are attributed either to the fact that the experimental accuracy of the temperature measurement is only of the order of

Messrs. Honeywell-Brown, Ltd., of Perivale, Middlesex, for the loan of the high-speed recording potentiometer described in this paper.

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Ten-Ton Ingot Moulds

A COMPARISON OF DESIGN AND CONDITIONS OF USE

By A. Jackson, A.Met., F.I.M.

SYNOPSIS

The design and life of two types of ten-ton mould and bottom plate are compared under normal conditions of use, and, for the later type, the results of detailed experiments, designed to indicate the effect of certain variables in the conditions of use, are shown. Both types produce the same ingot size, but the mould design was later modified and improved by alterations based on the failings of the earlier design. The effects of short and long tap-to-strip times and normal and long cooling times between heats are evaluated.

The various types of failure are discussed and conclusions indicate the possibility of very high mould lives being achieved, provided stripping is sufficiently rapid.

Experiments are described on the heating and cooling rates of moulds, the effects of annealing on mould life, and the effect of order of manufacture, and other miscellaneous data.

Introduction

THE two types of mould considered are referred to as 10-ton O.T. (open top) and 10-ton T.T. (taper top) moulds. Each type has its own design of bottom plate : that for the 10-ton O.T. has a shallow central dish, as is common in several types of mould, and the as-cast surface is in contact with the mould, whereas the 10-ton T.T. mould has a machined face on both base of mould and upper surface of bottom plate. The two are located by lugs and spigots, and the dish in the plate coincides exactly with the inner cross-section of the bottom of the mould. The mould and plate designs are illustrated in Figs. 1 and 2.

Although the 10-ton O.T. mould has a low mould consumption per ton of steel, efforts to give further economies in mould-metal consumption resulted in the following changes in design :

(1) The upper walls were thickened to give a taper top, to prevent the formation of vertical cracks on the top narrow side of the mould. This aim was achieved, and the resulting overhang also helped to avoid steel pouring on to the mould wall because of careless teeming.

(2) A lug on the narrow side, at the bottom, was necessary to allow the locating pins on the bottom plate to register. The added strength in these mould lugs was expected to prevent vertical cracks from forming on the narrow side. This was not achieved, although the incidence of cracking was reduced.

(3) The broadside of the mould was thickened, and a band 1 in. thick and 6 in. deep was cast at the base to reduce cracking on this side.

(4) The bottom plates for the 10-ton T.T. design were made to carry two moulds, in place of the former design which carried one. This new plate was much heavier per ton of steel cast. It was also thicker to enable a deeper cone to be produced, stronger to avoid early cracking, and, probably the most important feature, its length was so designed that the moulds on the cars were uniformly spaced throughout the heat, and could not be bumped during shunting. Bumping would allow hot faces to come into contact and so reduce mould life.

The bottom-plate designs indicate that teeming in all cases was necessarily direct.

The average weight of a full ingot in the 10-ton T.T. mould is 10·05 tons. The maximum and minimum weights are 9 tons 9 cwt. 2 qtr. and 8 tons 9 cwt. respectively. These figures apply only to the moulds used in this test and give an M/I ratio of 0·94–0·89.

LIFE OF MOULDS

The results of a comparison between the old design, the 10-ton O.T. mould, and the new design, the 10-ton T.T. mould, in ordinary comparable practice are shown in Table I. Moulds failing by vertical cracking on the narrow side (*see* Table XI) are excluded. The tap-to-strip time given is not the average for all the moulds discarded, but is the

Paper SM/BA/86/50 of the Ingot Moulds Sub-Committee of the Steelmaking Division of the British Iron and Steel Research Association, received 28th August, 1950. The views expressed are the author's and are not necessarily endorsed by the Sub-Committee as a body.

Mr. Jackson is Deputy General Works Manager at the Appleby-Frodingham Steel Company, Scunthorpe.

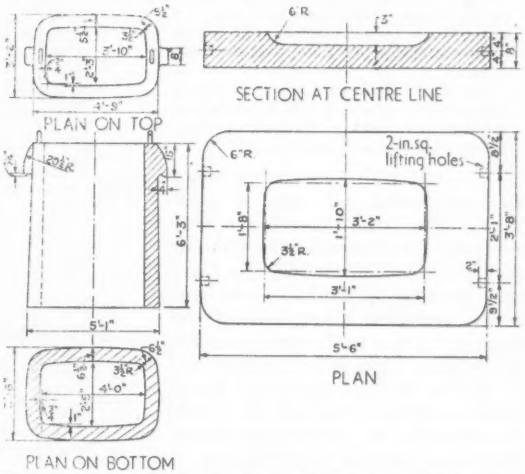


Fig. 1—10-ton short open-top mould: (*a*) Mould design; (*b*) bottom-plate design

Table I
MOULD LIFE AND MOULD CONSUMPTION

Period Ending	10-Ton O.T. Mould			10-Ton T.T. Mould			Average Tap-to-Strip Time, min.
	No. Discarded	Average Life	Mould Consumption, lb./ton	No. Discarded	Average Life	Mould Consumption, lb./ton	
28. 8.48	46	103	17·3				85
25. 9.48	23	106	17·0				70
23.10.48	28	103	17·3				70
20.11.48	28	101	17·4 (Average 17·5)				73
18.12.48	26	102	17·4				74
15. 1.49	19	92	19·4				84
12. 2.49	18	106	17·0	13	129	16·0	70
9. 4.49				5	126	16·3	74
7. 5.49				18	142	14·3	66
4. 6.49				14	149	13·8	65
2. 7.49				15	160	12·9	64
30. 7.49				10	165	12·5 (Average 14·6)	64
27. 8.49				28	149	13·8	81
24. 9.49				15	133	15·6	72
22.10.49				32	133	15·6	73
19.11.49				12	139	14·5	65
17.12.49				24	130	15·9	68
14. 1.50				24	138	15·0	71

average for all moulds used in the four-weekly period. It is shown here to give some continuity to the comparison between the old and new mould types being considered. The results show two major points, namely, that both designs, judged by any standard, have very good lives, and that the new design of mould gives additional economies in mould- and bottom-plate consumption, the latter being reduced by almost 75%.

It will be shown later that user conditions can give further considerable economies.

Effect of Conditions of Use

Trials on the new 10-ton T.T. were designed to evaluate the effect of the following factors on mould life :

(1) Position of the mould in the heat during teeming
(2) The effect of long and short tap-to-strip times on the life of moulds at approximately the same temperature, when receiving the heat of steel
(3) The effect of long and short tap-to-strip times on mould life when moulds are cold. (Standing time between heats not less than 36 hr.)

The trials with long cooling times were continued for over a year, during which the other trials were repeated on two occasions. All trials were observed continuously by three men working in shifts, whose sole duties consisted of organizing and supervising the trials and recording the results. They were fully qualified to do this kind of work and maintained constant conditions as closely as is possible in practical operation. They also recorded all deviations.

The fundamental reason for making these tests was to estimate the monetary value of increased mould life arising from more rapid stripping. Only the effect on mould life is recorded here.

Test Details

In all cases, pieces of steel plate approximately 15 in. × 15 in. × ¾ in. were placed in the mould to protect the bottom plate, and a circular splash-can of about 18 in. dia. and 18 in. high was used. There was no mould dressing, but the moulds were ' reeked ' by burning in the bottom of them some cotton waste saturated in a mixture of paraffin and thin tar. The moulds were covered during this treatment, and the inside surface became coated with soot.

The average time taken to fill each mould was approximately 3 min.

Six 10-ton moulds, disposed on three casting cars,

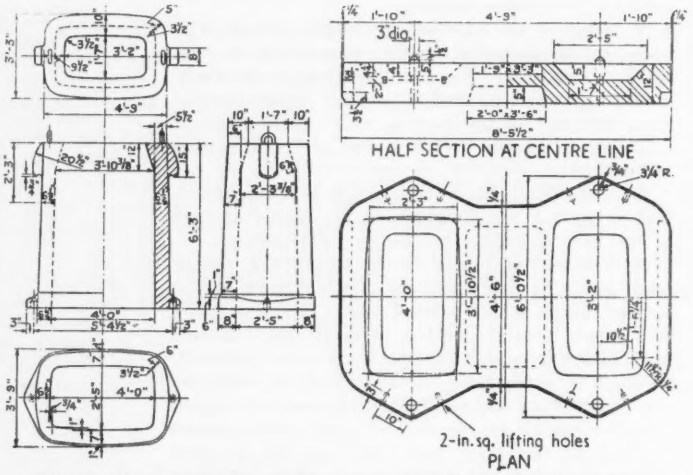


Fig. 2—10-ton cone-bottom taper-top mould: (*a*) Mould design; (*b*) bottom-plate design

were used in each trial heat. A fourth car contained one 10-ton and one 6-ton mould, but it was not included in the test. The moulds were numbered 1–6, and were always set up and cast in the same order and with the same sides adjacent at each tap. No. 1 mould had one hot side only, the others had two.

Two heats were in use, the first being stripped as quickly as possible after tapping, after a minimum time of 30 min. The second was held back, and stripped after 120 min.

The strip-to-tap time of these two heats varied; it was found that, with rapid stripping, the moulds were ready for the next tap in 8 hr., but with the long

Table II

EFFECT OF STEEL COMPOSITION ON MOULD PERFORMANCE

Heat No.	Order of Teeming	Maker's No.	Mould Weight, tons cwt. qtr.	Maker's Analysis					Life	Mode of Failure*
				T.C., %	Si, %	S, %	Mn, %	P, %		
Short Tap-to-Strip and Normal Strip-to-Tap Time										
A_1	1	10	9 4 0	3·57	1·36	0·058	0·62	0·074	229	(a)
	2	19	9 9 2	3·60	1·38	0·064	0·65	0·078	229	(a)
	3	15	9 5 0	3·63	1·60	0·064	0·72	0·050	216	(a)
	4	18	9 4 0	3·63	1·68	0·075	0·92	0·059	100	(c)
	5	24	9 4 2	3·66	1·48	0·069	0·60	0·050	221	(a)
	6	13	9 1 2	3·60	1·72	0·058	0·71	0·058	228	(a)
A_2	1	107	9 5 0	3·93	1·68	0·064	0·72	0·070	210	(a)
	2	108	9 3 2	3·71	1·60	0·070	0·64	0·080	141	(c)
	3	88	9 5 0	3·95	1·67	0·070	0·79	0·050	196	(b)
	4	104	9 1 2	3·99	1·72	0·070	0·83	0·060	211	(a)
	5	35	9 8 2	3·63	1·48	0·074	0·70	0·060	211	(a)
	6	36	9 8 2	3·63	1·68	0·076	0·89	0·080	208	(a)
A_3	1	202	9 2 0	3·78	1·88	0·067	0·76	0·060	221	(a)
	2	229	9 3 0	3·81	1·76	0·058	0·78	0·060	220	(a)
	3	215	9 7 2	3·97	1·88	0·060	0·80	0·080	185	(a)
	4	232	9 5 0	3·81	1·72	0·073	0·78	0·070	185	(a)
	5	236	9 4 2	3·79	1·60	0·056	0·65	0·060	224	(a)
	6	233	9 7 0	3·79	1·52	0·077	0·74	0·070	250	(a)
Long Tap-to-Strip and Normal Strip-to-Tap Time										
B_1	1	71	9 4 0	3·42	1·60	0·076	0·64	0·070	115	(a)
	2	61	9 3 2	3·63	1·68	0·064	0·56	0·070	115	(a)
	3	79	9 6 0	3·71	1·84	0·064	0·91	0·080	110	(a)
	4	91	9 6 0	3·81	1·60	0·056	0·86	0·070	114	(a)
	5	99	8 18 0	3·69	1·68	0·074	0·79	0·050	109	(a)
	6	95	9 2 2	3·90	1·76	0·061	0·85	0·060	114	(a)
B_2	1	94	9 2 2	3·70	1·76	0·052	0·77	0·070	110	(a)
	2	97	9 4 2	3·63	1·76	0·062	0·79	0·070	110	(a)
	3	110	9 4 0	3·86	1·62	0·064	0·75	0·050	111	(a)
	4	90	9 4 2	3·90	1·68	0·058	0·87	0·070	108	(a)
	5	98	9 5 2	3·69	1·68	0·074	0·79	0·050	109	(a)
	6	65	9 4 2	3·60	1·80	0·074	0·74	0·070	106	(a)
B_3	1	231	9 6 0	3·85	1·51	0·066	0·71	0·070	131	(a)
	2	205	9 4 0	3·96	1·84	0·070	0·81	0·060	77	(c)
	3	228	9 4 0	3·87	1·67	0·065	0·72	0·050	113	(a)
	4	203	9 1 2	3·78	1·88	0·067	0·76	0·060	83	(b)
	5	225	9 7 0	3·76	1·60	0·079	0·78	0·050	114	(a)
	6	201	9 4 2	3·78	1·60	0·079	0·79	0·070	112	(a)
Short Tap-to-Strip Time with Cold Moulds										
C	1	89	9 4 0	3·95	1·67	0·070	0·79	0·050	172	(a)
	2	16	9 5 2	3·63	1·52	0·080	0·68	0·068	176	(c)
	3	17	8 18 2	3·78	1·44	0·083	0·69	0·062	204	(a)
	4	77	9 3 0	4·02	2·00	0·060	0·77	0·050	193	(b)
	5	105	9 3 0	3·90	1·70	0·070	0·73	0·060	202	(a)
	6	116	9 4 2	3·72	1·76	0·070	0·73	0·070	201	(a)
Long Tap-to-Strip Time with Cold Moulds										
D	1	21	9 6 0	3·78	1·68	0·084	0·71	0·080	100	(a)
	2	20	9 5 0	3·66	1·40	0·064	0·66	0·066	101	(a)
	3	60	9 4 2	3·63	1·68	0·064	0·56	0·070	100	(a)
	4	51	9 3 2	3·82	1·72	0·070	0·87	0·060	101	(a)
	5	78	9 3 2	3·71	1·84	0·064	0·91	0·080	98	(b)
	6	75	9 3 2	3·66	1·60	0·098	0·72	0·060	86	(c)

* (a) = Horizontal crack on broad side (b) = Vertical crack on broad side (c) = Vertical crack on narrow side

Table III—MOULD LIFE AND MODE OF FAILURE*

	Heat A_1						Heat A_2						Heat A_3						Heat B_1					
Mould No.	1	2	3	4	5	6	1	2	3	4	5	6	1	2	3	4	5	6	1	2	3	4	5	6
Mould life	229	229	216	100	221	228	210	141	196	211	211	208	221	220	185	185	224	250	115	115	110	114	109	114
Mode of failure	(a)	(a)	(a)	(c)	(a)	(a)	(a)	(c)	(b)	(a)	(a)	(a)	(a)	(a)	(a)	(a)	(a)	(a)	(a)	(a)	(a)	(a)	(a)	(a)
Wt. of mould per ton of ingots, lb.	9·1	9·3	9·6	20·5	9·3	8·9	9·9	14·5	10·4	9·5	10·0	10·1	9·1	9·2	11·2	11·1	9·1	8·4	18·1	18·0	18·8	18·2	18·1	18·0
Mean				10·2						11·4						9·6						18·2		
Average life of moulds				204						196						214						113		
No. of failure of type (a)				5						4						6						6		
Average life				225						210						214						113		
No. of failure of type (b)				..						1						..						..		
Average life				..						196						..						..		
No. of failure of type (c)				1						1						..						..		
Average life				100						141						..						..		
Trials commenced				November, 1948						March, 1949						May, 1949						November, 1948		
Trials ended				February, 1949						June, 1949						September, 1949						February, 1949		

	Heat B_2						Heat B_3						Heat C						Heat D					
Mould No.	1	2	3	4	5	6	1	2	3	4	5	6	1	2	3	4	5	6	1	2	3	4	5	6
Mould life	110	110	111	108	109	106	131	77	113	83	114	112	172	176	204	193	202	201	100	101	100	101	98	86
Mode of failure	(a)	(a)	(a)	(a)	(a)	(a)	(a)	(c)	(a)	(b)	(a)	(a)	(a)	(c)	(a)	(b)	(a)	(a)	(a)	(a)	(a)	(a)	(b)	(c)
Wt. of mould per ton of ingots, lb.	18·4	18·6	18·3	18·9	18·8	19·3	16·0	26·5	18·2	24·2	18·4	19·0				11·1						20·8		
Mean				18·7						19·8														
Average life of moulds				109						105						191						98		
No. of failure of type (a)				6						4						4						4		
Average life				109						118						195						101		
No. of failure of type (b)				..						1						1						1		
Average life				..						183						193						98		
No. of failure of type (c)				..						1						1						1		
Average life				..						77						176						86		
Trials commenced				February, 1949						April, 1949						November, 1948						November, 1948		
Trials ended				April, 1949						August, 1949						December, 1949						June, 1949		

* (a) = Failure by wear of the inside surface, with horizontal cracks on the broad side, later extending vertically downwards
(b) = Failure by extended vertical cracks on the broad side
(c) = Failure by vertical cracks on the narrow, or lug, side

Table IV—TIME OF METAL IN MOULD, AND MEAN TIME FROM STRIP TO TAP

	Heat *A*		Heat *B*		Heat A_2		Heat B_1		Heat B_2		Heat B_3		Heat *C*		Heat *D*	
Mould No.	Time in Mould, min.	Time from Tap to Strip, hr.	Time in Mould, min.	Time from Tap to Strip, hr.	Time in Mould, min.	Time from Tap to Strip, hr.	Time in Mould, min.	Time from Tap to Strip, hr.	Time in Mould, min.	Time from Tap to Strip, hr.	Time in Mould, min.	Time from Tap to Strip, hr.	Time in Mould, min.	Time from Tap to Strip, hr.	Time in Mould, min.	Time from Tap to Strip, hr.
1	47	9·5	48	9·7	46	9·7	138	14·5	134	14·4	132	13·8	43	43·0	131	43·6
2	44	9·5	47	9·5	45	9·8	130	14·5	132	14·4	124	13·9	41	42·8	131	43·6
3	46	9·5	42	9·7	43	9·8	128	14·5	130	14·4	123	13·8	40	45·3	127	43·6
4	40	9·5	42	9·7	44	9·8	127	14·5	129	14·5	121	13·9	39	45·3	127	43·6
5	46	9·5	39	9·7	40	9·8	128	14·5	128	14·5	121	13·9	38	45·3	125	43·6
6	41	9·5	39	9·7	44	9·6	125	14·5	127	14·7	121	14·0	37	45·3	125	43·6
Mean	44	9·5	43	9·7	44	9·8	129	14·5	130	14·5	124	13·9	40	44·5	128	43·6

stripping time, 12-hr. cooling was necessary for the mould to reach the same temperature. These figures were derived from the test results on heating and cooling rates shown later. Two further heats were stripped in approximately 30 and 120 min. but were allowed to stand at least 36 hr. between taps to ensure that they cooled down to atmospheric temperature.

In a steelworks, moulds can be used only when furnaces are tapped, and stripping of a trial heat may be delayed by other factors. General instructions were given that the interval when the warm mould was not in use must not be greatly reduced, but that the operator should wait for the next tap ; if alteration in stripping time was necessary, the short time should be reduced and the longer time increased.

All moulds used in this test were first examined to ensure freedom from such obvious physical defects

Table V

PERCENTAGE OF MOULDS USED AT VARIOUS TAP-TO-STRIP TIMES

Trial No.	Mould		Average Tap-to-Strip Time, min.	Time Ingots in Mould											
	Maker's No.	Position		20–29 min.	30–39 min.	40–49 min.	50–59 min.	60–69 min.	<110 min.	110–119 min.	120–129 min.	130–139 min.	140–149 min.	>150 min.
	10	1	47	5·7	31·9	29·3	20·5	12·6						
	19	2	44	14·8	30·1	30·0	17·5	7·4						
A_1	15	3	46	15·9	26·5	26·8	16·7	12·5						
	18	4	40	30·0	33·0	24·0	8·0	4·0						
	24	5	46	15·4	30·3	26·2	15·4	12·6						
	13	6	41	26·3	28·5	27·2	11·0	7·0						
	107	1	48	5·7	37·9	24·9	15·3	16·3						
	108	2	47	14·9	51·1	21·3	7·1	5·7						
A_2	88	3	42	16·8	39·8	23·5	10·7	9·2						
	104	4	42	20·8	35·1	18·5	13·3	13·3						
	35	5	39	26·6	37·9	16·1	9·9	9·5						
	36	6	39	29·8	35·1	15·3	9·6	10·1						
	202	1	46	10·5	31·4	25·0	16·8	16·4						
	229	2	45	19·1	30·0	22·7	15·5	12·7						
A_3	215	3	43	24·9	32·4	18·4	10·8	13·5						
	232	4	44	25·4	28·1	22·2	13·0	11·4						
	236	5	40	32·1	30·4	18·3	11·7	7·6						
	233	6	44	34·0	28·8	17·2	10·0	10·0						
	71	1	138						4·4	15·6	43·5	16·5	10·4	9·5
	61	2	130						4·3	22·6	40·0	14·8	10·4	7·8
B_1	79	3	128						6·3	27·3	37·3	14·5	7·2	7·3
	91	4	127						7·0	32·5	33·3	13·2	7·0	7·0
	99	5	128						8·2	37·6	30·3	11·0	5·5	7·3
	95	6	125						10·5	41·2	24·6	13·2	3·5	7·0
	94	1	134						2·7	5·4	49·1	24·5	8·2	10·0
	97	2	132						2·7	7·3	53·6	20·9	5·4	10·0
B_2	110	3	130						2·7	17·1	53·1	12·6	6·3	8·1
	90	4	129						3·7	19·4	50·0	12·9	4·6	9·2
	98	5	128						4·6	27·5	40·4	14·6	4·6	8·2
	65	6	127						5·6	33·8	37·6	9·4	3·7	9·4
	231	1	132						7·7	15·4	42·3	25·4	4·6	4·6
	205	2	124						5·2	16·9	54·5	16·9	3·9	2·6
B_3	228	3	123						8·8	15·9	57·5	10·6	4·4	2·7
	203	4	121						9·6	24·1	53·0	6·0	6·0	1·2
	225	5	121						12·3	30·7	44·7	6·1	4·4	1·8
	201	6	121						14·3	37·5	37·5	3·4	4·5	2·7
				Moulds with Longer Cooling Times										
	89	1	43	11·0	41·8	26·2	10·0	11·0						
	16	2	41	17·6	44·3	20·5	8·0	9·6						
C	17	3	40	27·9	39·2	16·7	8·8	7·4						
	77	4	39	35·2	34·2	14·5	9·3	6·8						
	105	5	38	36·6	35·1	13·9	8·0	6·4						
	116	6	37	37·3	34·8	12·4	9·5	6·0						
	21	1	131					1·0	3·0	7·0	48·0	27·0	6·0	9·0
	20	2	131					1·0	3·0	13·9	45·5	23·8	5·7	7·9
	60	3	127					1·0	5·0	17·0	50·0	16·0	3·0	9·0
D	51	4	127					1·0	5·9	24·8	42·6	14·9	4·9	7·9
	78	5	125					1·0	6·1	29·6	39·8	11·2	6·1	7·1
	75	6	125					1·2	7·0	33·7	36·0	9·3	6·9	6·9

Table VI
PERCENTAGE OF MOULDS USED AT VARIOUS STRIP-TO-TAP TIMES

Trial No.	Mould		Average Strip-to-Tap Time, hr.	Strip-to-Tap Time								
	Maker's No.	Position		< 6 hr.	6 hr.	7 – 8 hr.	9 – 10 hr.	11 – 12 hr.	13 – 14 hr.	15 – 16 hr.	17 – 18 hr.	19 – 20 hr.
	10	1	9·5		3·9	38·6	36·4	13·2	4·4	3·5		
	19	2	9·5		3·9	38·6	36·4	13·2	4·4	3·5		
	15	3	9·5		3·7	38·6	36·7	13·5	4·2	3·3		
A_1	18	4	9·5		2·0	39·4	37·4	13·1	6·1	2·0		
	24	5	9·5		3·6	37·7	36·8	13·6	4·6	3·6		
	13	6	9·5		4·0	38·8	36·6	12·8	4·4	3·5		
	107	1	9·7	1·4	7·7	36·8	29·2	13·4	6·2	5·3		
	108	2	9·5	1·4	7·9	36·4	29·3	14·3	7·1	3·6		
A_2	88	3	9·7	1·5	8·2	35·4	29·2	13·8	6·1	5·7		
	104	4	9·7	1·4	7·6	37·1	29·1	13·3	6·2	5·2		
	35	5	9·7	1·4	7·6	37·1	29·1	13·3	6·2	5·2		
	36	6	9·7	1·4	7·7	36·7	28·5	13·5	6·3	5·8		
	202	1	9·7		6·4	37·4	32·8	13·2	7·8	2·3		
	229	2	9·8		6·4	37·2	33·0	13·3	7·8	2·4		
A_3	215	3	9·8		4·8	38·3	33·3	13·7	7·7	2·2		
	232	4	9·8		4·8	38·3	33·3	13·7	7·7	2·2		
	236	5	9·8		6·8	37·4	32·9	13·1	7·7	2·3		
	233	6	9·6		6·9	39·5	31·1	12·9	7·3	2·4		
	71	1	14·5				5·3	33·3	29·8	14·9	7·9	7·9
	61	2	14·5				5·3	33·3	28·9	14·9	8·8	6·1
B_1	79	3	14·5				5·5	32·1	28·4	15·6	9·2	8·3
	91	4	14·5				5·3	33·6	27·4	15·0	8·8	8·8
	99	5	14·5				5·6	31·5	26·8	16·7	9·2	9·4
	95	6	14·5				5·3	33·6	27·2	15·0	8·8	9·8
	94	1	14·4				3·7	21·1	24·8	20·2	16·5	13·7
	97	2	14·4				3·7	21·1	24·8	20·2	16·5	13·7
B_2	110	3	14·4				3·6	20·9	24·5	20·9	16·4	13·6
	90	4	14·5				3·7	21·5	28·0	20·6	15·9	13·1
	98	5	14·5				3·7	21·3	25·0	20·4	15·7	13·9
	65	6	14·7				3·8	21·9	23·8	19·0	16·8	15·2
	231	1	13·8				14·7	31·4	20·8	16·2	9·2	7·7
	205	2	13·9				13·2	31·6	18·4	21·1	9·2	6·6
B_3	228	3	13·8				14·3	28·6	24·1	16·1	9·8	7·1
	203	4	13·9				13·4	30·5	19·5	20·7	8·5	7·3
	225	5	13·9				14·2	28·3	23·9	15·0	9·7	8·9
	201	6	14·0				14·4	27·9	24·3	15·3	9·0	9·0

				Moulds with Longer Cooling Time								
				< 36 hr.	37 – 38 hr.	39 – 40 hr.	41 – 42 hr.	43 – 44 hr.	45 – 46 hr.	47 – 48 hr.	49 – 50 hr.	> 50 hr.
	89	1	43·0	3·0	12·2	26·3	17·0	20·5	9·3	4·7	2·3	4·7
	16	2	42·8	4·5	11·4	27·0	17·1	17·1	9·2	5·1	2·3	6·3
	17	3	45·3	3·0	10·3	23·7	19·3	15·3	8·4	5·9	3·3	10·8
C	77	4	45·3	3·1	10·9	24·5	16·6	17·2	9·4	5·7	2·6	10·0
	105	5	45·3	3·0	10·4	24·0	15·4	17·4	9·4	6·0	3·0	11·4
	116	6	45·3	3·5	10·5	23·5	15·5	17·5	9·5	5·5	3·0	11·5
	21	1	43·6	2·0	6·2	25·3	27·3	13·1	9·1	4·0	2·0	11·1
	20	2	43·6	2·0	6·0	24·0	27·0	13·0	10·0	4·0	2·0	11·0
	60	3	43·6	1·0	6·2	25·3	27·3	13·1	10·1	4·0	2·0	11·1
D	51	4	43·6	2·0	6·0	25·0	27·0	13·0	10·0	4·0	2·0	11·0
	78	5	43·6	1·0	6·2	24·7	26·8	12·4	11·3	4·1	3·1	10·3
	75	6	43·6	1·2	7·0	21·2	27·1	11·7	11·7	4·7	3·5	11·7

as laps, cold shuts, or top porosity. No special moulds were selected ; those used are given in Table II and were as normally made in the makers' foundry. Heats made up of consecutively manufactured moulds were avoided. All heats were marked to ensure that each set of moulds was kept on the same heat, and that the moulds were used in the same order, and not turned round during stripping. As each mould completed its life, it was replaced by another of the same type, but the performance was not recorded. Each heat, therefore, started as a full set and finished when the last single mould had completed its life. This method ensured that conditions of use were comparable over the greater part of the life of each mould.

For convenience in recording, the four types of trial are designated *A*, *B*, *C*, and *D*. A_1, A_2, and A_3 indicate the first, second, and third heats on trial *A* in which there was a short tap-to-strip time with a normal mould-cooling time. B_1, B_2, and B_3 are the first, second, and third heats on trial *B*, in which there was a tap-to-strip time of approximately 2 hr., and a normal cooling time.

C is the heat in which there was a short tap-to-strip time, but the mould was used cold on each occasion. The cooling time aimed at was 36 hr. or more.

D is the trial in which conditions were the same as in *C*, except that the tap-to-strip time aimed at was approximately 2 hr.

Trials *C* and *D* proceeded continuously from November 1948 to December 1949 and from November 1948 to June 1949 respectively, and therefore included both winter and summer conditions.

Discussion of Results

The information obtained was as follows :

(1) The temperature of the bath at tapping, and between first and second ladles.

(2) The steel quality, size of nozzle and ladle skull, the condition of the stopper, and whether the ladle is the first or subsequent ladle to be taken from the furnace.

(3) The time of filling each ladle, the exact time of filling each mould, and observations on pouring conditions.

(4) The time at which each ingot was stripped.

(5) A record of the development of mould defects. The mould was examined before each tap to see that it was correctly set on the bottom plate, complete with splash-can, etc.

Eight heats were examined : three had short, and three long, tap-to-strip times, and normal strip-to-tap times ; one had a short, and one a long, tap-to-strip time, but both had a sufficient strip-to-tap time to enable them to cool to atmospheric temperature before being used again.

Mould Life and Mode of Failure—Results in Table III indicate the mould life and mode of failure and their relationship for each trial heat. They show that the usual mode of failure was in the category 'horizontal crack on the broad side'—this category includes vertical cracks which commence from the horizontal. The life shows great consistency, in all instances, for this standard type of failure. Vertical cracks developing on the broad side occurred only in four cases and the differences in life, which are important to the user, can hardly be said to arise from a mode of failure which differs in principle from that of the most common type. Vertical cracking on the narrow side shows, in most instances, definite indications of premature failure at a life some measure below normal. Evidence seems to indicate that this particular type of failure is related to important factors other than those arising in the works of the user. Most of the moulds were lightly dressed during the course of their life. It has been suggested that mould life may be influenced by the time of the year in which the moulds are used. Periods of use are therefore included to give the seasonal variation.

The weight of mould per ton of ingots is calculated from the total weight of ingots made in each mould and the mould weight. Table III shows that in normal use a short tap-to-strip time gives a mould consumption of 10·4 lb./ton, whilst a long tap-to-strip time gives an average consumption of 18·9 lb./ton. The two casts with the long cooling time between use, have an increased consumption of about 10% over those with normal cooling time.

Time in Mould and Mean Time from Strip-to-Tap—Table IV shows the mean time between filling and stripping the mould. As it takes longer to teem the moulds than it does to strip them the figures show a falling off in the time in the mould from the first to the last mould teemed in each heat.

Table VII

PERCENTAGE OF MOULDS TAPPED AT GIVEN BATH TEMPERATURES

Temperature of Bath, °C.		Heat A_1	Heat A_2	Heat A_3	Heat B_1	Heat B_2	Heat B_3	Heat *C*	Heat *D*
Tapping	< 1580	13·9	8·9	4·9	10·8	13·7	10·5	10·0	9·2
	1580–1589	62·7	67·4	64·0	65·6	65·1	58·8	63·9	62·5
	1590–1599	21·7	23·2	29·1	23·6	21·2	29·8	26·0	24·2
	> 1600	1·7	0·5	2·0	...	...	0·9	0·1	4·1
	Mean Temp.	1583	1585	1587	1584	1584	1586	1586	1586
Between Ladles	< 1580	...	1·0	...	...	1·2	...	3·9	4·6
	1590–1589	24·6	19·4	21·2	23·6	29·1	21·7	21·3	13·4
	1590–1589	58·7	71·0	59·1	54·4	54·2	54·3	56·8	60·0
	> 1600	16·7	8·6	19·7	22·0	12·5	24·0	18·0	22·0
	Mean Temp.	1595	1593	1594	1595	1592	1594	1593	1596

One point should be noted in relation to No. 4 mould in heat A_1. This heat, because of its manner of crazing, had a steadily increasing stripping time which continued until the moulds were dressed at a life of about 150. As No. 4 mould failed before this increased time became significant, its average tap-to-strip time is less than that of the remainder of the heat.

The times of approximately 9·7 and 14·4 hr. are rather higher than was intended when the test commenced. The increase is due to the need to fit the mould usage time into the routine of furnace tapping, and as instructions were that longer rather than shorter times should be favoured, the cooling time was a little longer than planned. These times are still relatively of such an order as would give approximately the same mould temperature when the mould is used again.

A detailed summary showing the spread of tap-to-strip and strip-to-tap times is shown in Tables V and VI and illustrates the extent of the unavoidable variations in steelworks practice, even in a controlled test.

Tapping Temperature of Bath and Temperature Between Ladles—Table VII gives the average bath temperature at which the sample-passer commenced to tap the furnace. The temperature normally increases during the tapping operation, so that when the steel strikes the ladle it is a little hotter than the tapping temperature. To assess this increase the temperature was again taken between the first and second ladle. This should give the actual temperature of the steel as it is being teemed into the ladle, and a most valuable comparison from the viewpoint of ingot mould life can then be made.

Ladle and Ladle Skull—Table VIII shows the number of times each heat was used on the 1st, 2nd, 3rd, and 4th ladle. In large tilting furnaces, the temperature of the steel in the first ladle is normally slightly lower than that of the remainder because it comes from the bottom of the furnace. Also, the ladle takes a little longer to fill because of the time which elapses before the taphole is washed out to its full, ordinary size—usually 3 or 4 min. of the ladle filling time. The difference in temperature between the first and later ladles is not very great, but, in any event, the bath temperature taken between the first and second ladles gives quite a good estimate of the actual temperature of the steel entering the second and third ladles.

None of the ladles was covered with slag. The skull was recorded in every case by the observers in charge of the test. The normal aim for a satisfactory tapping temperature is a plate of skull practically covering the ladle bottom or, within the limits, part plate and one course of brickwork (6 in.). The group showing two to five courses of skull usually contains the majority of first ladles.

Stopper and Nozzle Size—Table IX indicates the appearance of the stream when entering the mould and is self-explanatory. 'Running' refers to heats which are completely out of control. Nozzles are fireclay, and are substantial (10½ in. in length), the clay containing 34% of alumina. Only one nozzle per ladle is used.

Quality—Table X indicates that most of the steel was between 0·12 and 0·20% carbon, balanced in type, and generally of as uniform quality as it is possible to make in a steelworks.

DEVELOPMENT OF DEFECTS

Crazing

The development of this defect was quite different in the groups with long and short times for ingots in the moulds.

With a short tap-to-strip time, crazing became visible after a longer life ; it consisted of a network of projections of about 0·1 in. in height, and bounded areas forming a mesh approx. 4–6 in. in dia. These projections were divided by a minute crack which was visible everywhere on the crazy pattern. The projecting crazy mesh became so pronounced that it was dressed off by grinding, at about 150 lives, because it was believed that the rough mould surface was causing the more difficult stripping then being experienced. The projecting pattern reappeared with further use and persisted to the end of the mould life. In contrast, the heats with long tap-to-strip times showed a crazy network of much smaller pattern, only about ½–1 in. in dia. In this case, however, the network consisted of depressions, thus probably conforming to the most frequently observed patterns in practice.

The above observations applied to all the eight heats examined. (*See* Figs. 3–5.)

Table VIII

PERCENTAGE OF MOULDS USED ON EACH LADLE AND AMOUNT OF LADLE SKULL

	No.	Heat A_1	Heat A_2	Heat A_3	Heat B_1	Heat B_2	Heat B_3	Heat C	Heat D
Ladle	1	36·9	36·2	55·9	32·2	30·0	21·1	38·5	26·7
	2	48·7	48·6	34·6	39·1	45·5	50·9	47·0	34·7
	3	11·7	12·4	9·4	23·5	21·8	25·4	13·5	29·7
	4	2·6	2·8	0·1	5·2	2·7	2·6	1·0	8·9
Ladle skull	Nil	1·7	3·3	0·9	3·5	...	2·6	1·0	4·0
	Part plate	8·7	7·6	6·2	19·1	14·5	14·9	8·5	16·7
	Plate	40·9	41·4	39·8	32·2	42·7	41·2	43·5	46·5
	1 Course	16·5	15·7	17·5	20·8	10·9	14·9	13·5	8·9
	2–5 Courses	23·4	21·4	26·6	18·3	23·6	13·2	24·0	14·9
	6–10 Courses	8·8	10·6	9·0	6·1	8·3	13·2	9·5	9·0

Fig. 3—Heat A_3, mould 1, showing projecting crazing at 150 lives; final life 220

Fig. 4—Heat B_2, mould 5, showing small depressed crazing at 75 lives; final life 109

(a)

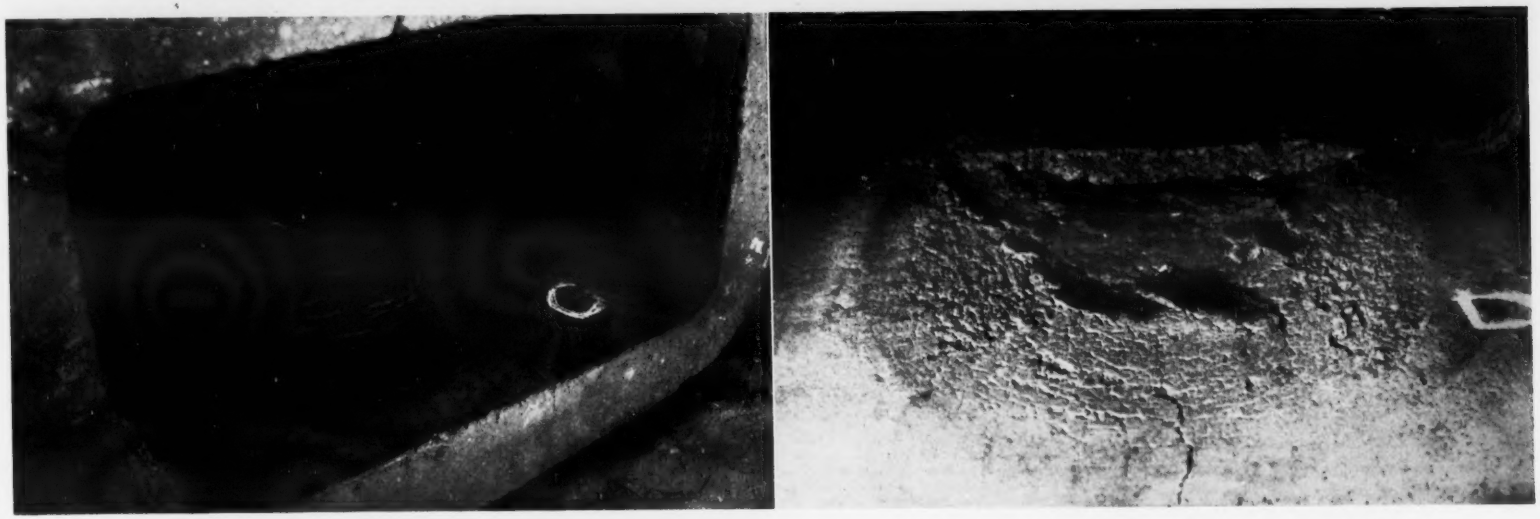
(b)

Fig. 5—Inner surface of moulds in heat A_2 showing vertical defects, starting from the bottom, that were visible before the mould had completed three-eighths of its life. Photographed at 150 lives : (*a*) Mould 5, final life 211 ; (*b*) mould 6, final life 208

(a) (b) (c)

Fig. 6—Failure of moulds by cracking : (*a*) Heat B_1, mould 5, showing horizontal and vertical cracking on broad side ; (*b*) heat *D*, mould 5, showing vertical cracking on broad side at final life of 98 ; (*c*) heat A_1, mould 4, showing vertical cracking on narrow side at final life of 100

(a)



(b)

Fig. 7—Final appearance of inside surface of moulds : (*a*) Heat A_1, mould 3, with short tap-to-strip time, final life 216 ; (*b*) heat B_1, **mould 4, with long tap-to-strip time, final life 114**

Table IX

PERCENTAGE OF MOULDS WITH GIVEN STOPPER PERFORMANCE AND NOZZLE SIZE

		Heat A_1	Heat A_2	Heat A_3	Heat B_1	Heat B_2	Heat B_3	Heat C	Heat D
Stopper	Good	96·5	94·8	90·4	94·7	98·2	95·7	93·2	95·0
	Dribbling	2·6	5·2	8·3	1·8	1·8	3·5	6·1	5·0
	Running*	0·9	...	1·3	3·5	...	0·8	0·7	...
Nozzle dia., in.	1	0·5	0·5	...	...	...	...	0·5	...
	1·25	25·6	39·5	33·6	33·1	40·0	50·0	35·5	45·6
	1·5	66·5	52·4	52·4	64·3	56·4	40·4	64·8	48·5
	2	7·4	7·6	14·0	2·6	3·6	9·6	9·2	5·9

* Refers to heats which are completely out of control

The ingot surfaces from the crazy moulds show depressions which coincide with projections in the mould surface and *vice versa*. This remark is included for emphasis, as literature has suggested that this is not always the case.

Cracking

Failure by cracking is grouped under three main headings :

(*a*) Horizontal cracking on the broad side
(*b*) Vertical cracking on the broad side
(*c*) Vertical cracking on the narrow side.

and photographs of typical examples are shown in Fig. 6.

The first of these gives the best life, and that of the second is only slightly inferior, but vertical cracking on the narrow side almost invariably gives a life below the average of moulds failing by the other types of cracking.

Horizontal Cracking on the Broad Side—This is the most prevalent cause of failure and is normally found at a height between half and two thirds that of the mould. In the latter part of the mould life a vertical crack not infrequently commences at the centre of the horizontal crack and extends quite rapidly, with successive heats, to the bottom of the mould. The portion of the mould below the horizontal crack usually bulges outwards, up to 0·25 in., as the vertical crack develops.

The effect of the crack on the failure of the inner surface differs fundamentally with short and long tap-to-strip times. In moulds which retain the hot ingot for a short period only, steel penetrates the crack, and this thin fin is torn from the ingot and retained in the crack. This was fully demonstrated in the first trial with the short tap-to-strip time. The horizontal crack was noted, and as life succeeded life, it appeared to grow an increasing lip on its lower edge. Stripping became less easy, the moulds were taken off, and examination and analysis showed that the lip consisted of layer upon layer of steel. A projection appeared on the lower side of the horizontal crack which seemed to be caused by the growth of the hematite surface of the mould. Examination showed it to consist of layers of steel. A sample was chipped out and gave the following analysis :

C 0·09% P 0·031% Mn 0·57% S 0·049%

The mould was levelled by flame dressing and grinding and was returned to service. In the case of the moulds with the long tap-to-strip interval, the ingot surface must have been cooler, and the mould surface much hotter when stripped. The mould surface below the crack was gradually 'scoured away,' presumably because the fin or projection on the ingot was much stronger than the hot surface of the mould, which was apparently carried away in ever-increasing quantities each time the ingot was stripped, until in some cases the cavity was as much as 2 in. deep (*see* Fig. 7).

The increased difficulty in stripping mentioned before, in connection with the rapidly stripped heat, meant that the ingot would not leave the mould by its own weight. Further cooling had to be allowed, as with the short tap-to-strip times the ingot top was not sufficiently strong to withstand the application of force by the stripper ram.

Vertical Cracking on the Broad Side—This occurred in four instances. It commenced at the bottom, to one side of the mould centre line, and generally curved towards the mould corner. The mould life was only slightly inferior to that of those that failed essentially because of horizontal cracking on the broad side.

Fig. 7—Final appearance of inside surface of moulds : (*a*) Heat A_1, mould 3, with short tap-to-strip time, final life 240 ; (*b*) heat B_1, mould 4, with long tap-to-strip time, final life 114

Table X

PERCENTAGE OF MOULDS OF VARIOUS STEEL QUALITIES

Type of Steel	Heat A_1	Heat A_2	Heat A_3	Heat B_1	Heat B_2	Heat B_3	Heat C	Heat D
06/08C Rimming	0·4	1·5	2·6	0·9	...	2·6	1·8	...
06/08 Balanced	1·8	3·3	2·4	5·2	5·5	7·0	1·8	8·9
10/11C Balanced	4·4	2·4	3·9	4·3	4·5	3·5	3·4	8·9
12/13C Balanced	12·0	11·4	8·5	16·5	17·3	19·3	11·5	19·8
14/16C Balanced	42·7	44·3	31·1	33·9	32·7	37·7	34·1	35·6
17/20C Balanced	38·7	37·1	51·5	39·2	40·0	29·9	47·4	26·8

Table XI
PERFORMANCE OF 10-TON T.T. MOULDS

Four Weeks Ending	Moulds Discarded because of Vertical Cracking of the Narrow Side		Moulds Discarded for Other Reasons	
	No.	Av. Life	No.	Av. Life
12. 2.49	4	76	13	129
9. 4.49	4	100	5	126
7. 5.49			18	142
4. 6.49	1	82	14	149
2. 7.49	1	80	15	160
30. 7.49			10	165
27. 8.49	3	75	28	149
24. 9.49	8	94	15	133
22.10.49			32	133
19.11.49	2	83	12	139
17.12.49	6	117	24	130
14. 1.50	3	74	24	138
Total	32	92	210	140

Vertical Cracking on the Narrow Side—This has been found to some degree in slab moulds for many years. It is almost invariably associated with a below-normal mould life and arises from causes not entirely controllable by the user. It has been found in the past that the average mould life varies approximately with the proportion failing due to vertical cracks on the narrow side ; individual examination has shown that the life for each type of failure has remained substantially constant.

This type of failure commences at the bottom of the mould on the narrow side and extends steadily upwards with successive casts until the crack width at the bottom becomes sufficient to allow steel to flow through. In the moulds described, this crack passes through the pin-hole flange, which is the thickest part of the mould. As yet no reason can be ascribed to this failure, but if it could be overcome an important increase in mould life would result. In this series of tests it is notable that one or more failures of this type occurred in each of the four trials.

The proportion of failures in normal practice due to vertical cracking on the narrow side of the mould, is shown in Table XI. The average life of all the 242 moulds shown in the Table is 134. The trial moulds are not included in these figures.

Effects of Mould Wear on Ingot Surface

Owing to the way in which mould wear took place, ingots from the later lives of heats with a long tap-to-strip time had a far worse surface appearance than those from rapidly stripped heats. Sample ingots were photographed, rolled to slabs, photographed again and finally rolled to plates, often without producing any defective specimens, which could be attributed to the rough ingot surface. It is concluded that the effect of mould surface in producing defective specimens was small compared to the effects of cracks due to steel temperature, composition, or teeming speed, and of shell arising from faulty teeming, or of defects due to sub-surface blowholes, which present unsolved problems.

The number of tests reported may appear by some standards to be small, but it is believed that the care with which they were made gives practical significance to the conclusions enumerated.

Effect of Order of Teeming on Mould Life

This did not show any important effect on mould life, but the performance of the No. 1 mould in the various trials should be noted.

In no instance did the first mould fail prematurely but neither did it show better average lives than comparable moulds, with similar types of failure, teemed later in the heat. The No. 1 mould had obviously a ' hot ' and ' cold ' side in use, but with the ample spacing used in these heats this could hardly be expected to have had any appreciable effect on failure. Examination showed, however, that in six of the eight heats investigated, the outward facing ' cold ' side of the mould was less defective than the ' hot ' side, the latter being responsible for failure. The difference was least in the heats with the longer tap-to-strip times, but with rapid stripping the indications were that if both sides had remained as good as the ' cold ' side, the mould life would have been even longer.

This result was unexpected, and indicates the possibility, with short tap-to-strip time, of small changes in mould temperature having a considerable effect on mould life.

Dressing of Mould Defects

It was found that defects in moulds with a short tap-to-strip time ultimately developed sufficiently so as to prevent the ingot slipping from the mould when the latter was lifted and therefore stripping time had to be delayed by allowing a little extra cooling time. To obviate this, the mould was dressed, and to ensure uniformity in the remaining heats all were ultimately dressed except heat *D*, which was already out of use by the time the normal dressing life had been reached. Dressing consisted of removing projections which were above the normal mould surface level with a compressed air chipper, and of buffing the surface with a grinding wheel. The crazing was also ground down flat, although it continued to grow with further use.

In the moulds used with short tap-to-strip times the horizontal crack contained layers of steel which were cut off with an oxy-acetylene burner, and afterwards ground with the abrasive wheel. Dressing took place at the times shown below :

Heat	Life	Heat	Life
A_1	155	B_2	100
A_2	155	B_3	100
A_3	150	C	150
B_1	100	D	Not dressed

HEATING AND COOLING OF 10-TON T.T. MOULD

Tests were designed to assess the cooling time necessary to bring the temperature of two moulds to about the same level, after they had contained hot steel for different times.

The method of investigation is described to enable those who are interested to make their own estimate of the validity of the curves shown.

The outer wall temperature of the mould was obtained by applying a base metal contact thermo-

couple to three points on the mould, one in the centre of each broad side and one in the centre of one narrow side only. These points were clearly marked, and, before use, were levelled by filing. The mould tested was, in each case, the first of the heat and had therefore only one broad side facing the second mould of the cast. The other broad side, like the narrow side, could radiate freely to the atmosphere. Readings commenced immediately the first two moulds were full of molten steel and continued throughout stripping until the moulds were cool. The stripped mould was placed on a casting car, in conformity with general practice, and not cooled on the rack available for moulds that are used infrequently.

After preliminary tests, four trials were made, two with long and two with short tap-to-strip times. With the short stripping times the mould obviously did not reach such a high temperature but, in general, the trials were in good agreement and only one set of curves is shown in Fig. 8 (lower). The ingot was allowed to remain in the mould for 180 min.

In each test, the narrow side first attained a higher temperature, but after about two hours, the outer broad side reached that temperature and these two positions remained at the same temperature. The temperature of the broad side facing the adjacent hot mould continued to rise at the same rate as the other two sides for the first two hours, but then it continued rising at the same rate whilst that of the other sides tended to stabilize. With the mould spacings used, and under the conditions of the test, it would appear that heat from adjacent moulds has little effect on the lives obtained during these trials, as the tap-to-strip time seldom exceeded two hours by any appreciable amount.

An average heating curve is shown in Fig. 8 (upper) and an average cooling curve is shown in Fig. 9. The latter indicates that the cooling time for moulds stripped in 30 min. and 120 min. is approximately 8 hr. and 11½ hr. respectively. It is again emphasized that these results are approximate, but for works purposes they are sufficiently accurate to enable a satisfactory estimate to be made of the cooling time required by moulds with differing tap-to-strip intervals. Tests made in October, and based on the

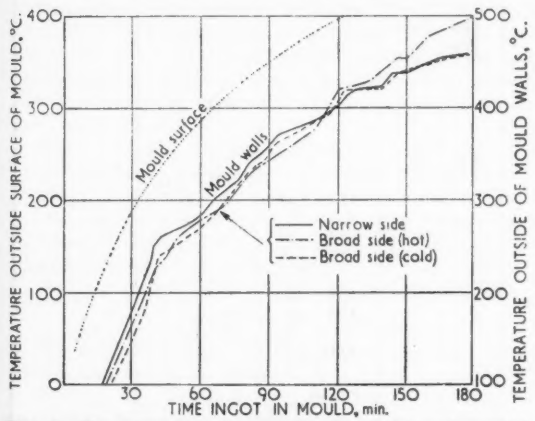


Fig. 8—Temperature of outside of mould and mould walls during heating up

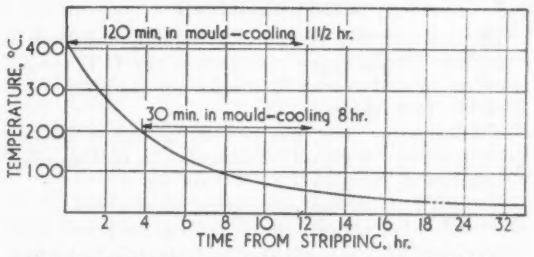


Fig. 9—Cooling rate of 10-ton taper-top moulds

mercury thermometer, showed that a cooling time of 36–42 hr. was necessary to ensure that the mould temperature fell to 20° C.

ANNEALED INGOT MOULDS

In this trial, six moulds were annealed in the ingot preheater. Three were used on a heat with a short tap-to-strip time and the other three on a long tap-to-strip heat. The six moulds selected were, like those in the other trials, apparently free from surface defects, laps, etc. These moulds were placed on the preheater bogies with their open ends facing the gas inlets, thus allowing hot gases to pass both inside and outside, and ensuring, as far as possible, uniformity of heating. The hot zone of the furnace was adjusted to 800° C., which gave a mould temperature in this zone of 700–750° C. The moulds remained at this temperature for two hours, and were checked by a Cambridge pyrometer. Every effort was made to keep the preheater at a steady temperature. The bogies containing the ingot moulds were followed by cold ingots, thus preventing the cool end of the furnace from heating to temperatures above normal.

Three annealed moulds, located in positions 1, 3, and 5, or 2, 4, and 6, were used on each of two 10-ton heats. With long tap-to-strip times, these moulds had an average life of 118 (131, 113, 114). Of the other three moulds, one failed with a vertical crack on the narrow side at 77 lives, one with a vertical crack on the broad side at 83 lives, and the other failed normally with a horizontal crack on the broad side at 112 lives. The other heat was used with short tap-to-strip times, and the three annealed moulds gave an average life of 219 (220, 185, 250). The other three, which were not annealed, failed normally with an average life of 210.

It is difficult to draw any conclusions from these figures, although it may be noted that none of the annealed moulds failed prematurely, and also that their average life was higher than that of the moulds which failed normally in the heats on which they were used. It must be emphasized that these results only indicate that further trials may be desirable.

EFFECT OF ORDER OF MANUFACTURE

Table XII shows the mould lives in the various heats set out in order of manufacturer's serial number. The type of failure is shown by the code letter, in brackets, as used in Table II. The figures indicate that the lives of moulds made at approximately the same time are related to usage and apparently not to date of manufacture. The life of premature failures also varies materially with conditions of use.

CONCLUSIONS

The introduction of the T.T. mould, designed to overcome certain types of failure in the O.T. mould, resulted in a decrease in mould consumption from 17·5 to 14·6 lb./ton.

Similarly, the new and heavier design of bottom plate produced a rather striking fall in bottom-plate consumption from 15·1 to 4·0 lb./ton.

Using T.T. moulds at a temperature of about 50° C., and with other conditions as uniform as practicable, decreasing the tap-to-strip time from 127 to 44 min. decreased the mould consumption, with the usual method of failure, from 18·2 to 9·6 lb./ton. If the moulds were allowed to cool for at least 36 hr. between heats, being used each time at atmospheric temperature, their life was reduced by about 10% with both long and short tap-to-strip times. Moulds with the long tap-to-strip time required about 45% longer cooling time than those with the short, to fall to the same temperature (about 50° C.); it is obvious that a constant tap-to-tap period for re-use does not mean constancy of operational conditions in practice.

Basically, in all tests, failure was normally caused by a horizontal crack on the broad side of the mould, frequently accompanied by a vertical crack growing downwards from the centre, but there was a fundamental difference in the type of crazing that developed. With the long tap-to-strip times, the crazy pattern was of small mesh and the markings were depressions. Rapid stripping, however, produced a very coarse mesh of projections, each subdivided by a fine crack. From some part of this pattern major failure started.

Table XII

EFFECT OF ORDER OF MANUFACTURE ON MOULD LIFE AND MODE OF FAILURE

Maker's Consecutive Number	A_1 Mould Life	A_1 Mode of Failure	A_2 Mould Life	A_2 Mode of Failure	A_3 Mould Life	A_3 Mode of Failure	B_1 Mould Life	B_1 Mode of Failure	B_2 Mould Life	B_2 Mode of Failure	B_3 Mould Life	B_3 Mode of Failure	C Mould Life	C Mode of Failure	D Mould Life	D Mode of Failure
10	229	(a)	...		...		...		...		...		...		...	
13	228	(a)	...		...		...		...		...		...		...	
15	216	(a)	...		...		...		...				...		...	
16	...		...		...		...		...		...		176	(c)	...	
17	...		...		...		...		...		...		204	(a)	...	
18	100	(c)	...		...		...		...		...		...		...	
19	229	(a)	...		...		...		...		...		...		...	
20	...		...		...		...		...		...		...		101	(a)
21	...		...		...		...		...		...		...		100	(a)
24	221	(a)	...		...		...		...		...		...		...	
35	...		211	(a)	...		...		...		...		...		...	
36	...		208	(a)	...		...		...		...		...		...	
51	...		...		...		...		...		...		...		101	(a)
60	...		...		...		...		...		...		...		100	(a)
61	...		...		...		115	(a)	...		...		...		...	
65	...		...		...		...		106	(a)	...		...		...	
71	...		...		...		115	(a)	...		...		...		...	
75	...		...		...		...		...		...		...		86	(c)
77	...		...		...		...		...		...		193	(b)	...	
78	...		...		...		...		...		...		...		98	(b)
79	...		...		...		110	(a)	...		...		...		...	
88	...		196	(b)	...		...		...		...		...		...	
89	...		...		...		...		...		...		172	(a)	...	
90	...		...		...		...		108	(a)	...		...		...	
91	...		...		...		114	(a)	...		...		...		...	
94	...		...		...		...		110	(a)	...		...		...	
95	...		...		...		114	(a)	...		...		...		...	
97	...		...		...		...		110	(a)	...		...		...	
98	...		...		...		...		109	(a)	...		...		...	
99	...		...		...		109	(a)	...		...		...		...	
104	...		211	(a)	...		...		...		...		...		...	
105	...		...		...		...		...		...		202	(a)	...	
107	...		210	(a)	...		...		...		...		...		...	
108	...		141	(c)	...		...		...		...		...		...	
110	...		...		...		...		111	(a)	...		...		...	
116	...		...		...		...		...		...		201	(a)	...	
201	...		...		...		...		...		112	(a)	...		...	
202	...		...		221	(a)	...		...		...		...		...	
203	...		...		...		...		...		83	(b)	...		...	
205	...		...		...		...		...		77	(c)	...		...	
215	...		...		185	(a)	...		...		...		...		...	
225	...		...		...		...		...		114	(a)	...		...	
228	...		...		...		...		...		113	(a)	...		...	
229	...		...		220	(a)	...		...		...		...		...	
231	...		...		...		...		...		131	(a)	...		...	
232	...		...		185	(a)	...		...		...		...		...	
233	...		...		250	(a)	...		...		...		...		...	
236	...		...		224	(a)	...		...		...		...		...	

(a) = Horizontal cracking on the broad side (b) = Vertical cracking on the broad side (c) = Vertical cracking on the narrow side

A few moulds failed prematurely owing to vertical cracks on the narrow side, and it is tentatively suggested that this type of failure has its origin outside the sphere of the user.

Neither order of teeming nor chemical analysis, within the limits shown, appear to have any important effect on mould life. There are indications that, had both sides of the first mould of each heat been 'cold,' then life would have been increased with rapid stripping.

The defective specimens produced in the plate mills from these trial casts were segregated from the general run and analysed in an endeavour to relate their number to mould life. For comparison they were placed into groups representing each quarter of the normal life. Examination of the figures failed to show any increase in the number of defective specimens with increasing mould life.

In the past it has been suggested frequently that it is necessary to scrap moulds because their worn inner surfaces produced increased numbers of defective steel specimens, and this was a reason for short mould lives. The figures shown here indicate that mould life by itself does not give a comparative indication of the state of the inside surface, *e.g.*, a mould which has a long tap-to-strip time with a life of 100 will have a much worse inner surface after 70 lives than a rapidly stripped mould after 120 lives.

Acknowledgments

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Effect of Initial Heating Temperature on the Mechanical Properties of Ni–Cr–Mo Steels

By J. A. Wheeler, B.Sc., Ph.D., V. Kondic, Ph.D., and T. Ko, B.Sc., Ph.D.

SYNOPSIS

The effect of high austenitizing temperature on the tensile properties of air-quenched Ni–Cr–Mo steels has been studied. Increasing the austenitizing temperature causes a progressive lowering of the ductility when the steel is fully martensitic after cooling. Heating to high temperatures prompts the formation of intermediate transformation products during subsequent cooling, with a consequent slight increase in the ductility and a decrease in the tensile strength. In one steel, precipitation of sulphide in a Widmanstätten pattern after being heated above 1350° C. causes pronounced loss of maximum stress and fracturing along $\{100\}$ planes, on which the inclusions were precipitated.

When an air-hardening steel is welded, the steel in the region next to the weld is heated to temperatures in the austenitic range, and on cooling transforms to martensite. The characteristics of the temperature cycle to which this material is subjected are unique, the maximum temperature attained, particularly, being outside all normal heat-treatment experience. Because cracking may occur in this hardened zone, it is important to investigate the effects of the thermal cycle on the mechanical properties of steels of the type with which welding difficulties are experienced.

The effects of the temperature of heating before quenching have been investigated for six Ni–Cr–Mo steels. The results on one of the steels (steel B) have previously been presented to the 1946 Symposium on the Metallurgy of Steel Welding, organized by the British Welding Research Association[1]; however, the important observations will be recapitulated here.

EXPERIMENTAL DETAILS

The analyses of the six steels used in the investigation are given in Table I.

Steels A, B, C, and E were received in the form of hot-rolled flats, which were found to have been cut from cross-rolled plates; these steels were annealed at 600° C. before machining. Steels VA and VB were received as ½-in. diameter hot-rolled round bars, and were used as received.

The apparatus used has previously been described in detail.[1] Essentially, it consisted of a water-cooled chamber *B* (Fig. 1), and a mullite furnace tube *T* which carried two furnaces—a Kanthal-wound low-temperature furnace *F*2, operated at 855 ± 5° C., and a platinum-wound high-temperature furnace *F*1. The temperature of furnace *F*1 was measured by a platinum/platinum–rhodium thermocouple placed in a mullite sheath *R*.

The water-cooled chamber and the furnace tube could be evacuated to a pressure of 0·1 mm. of Hg and then refilled with an inert gas. Nitrogen was used for steels B and C, and argon for the other

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Dr. Wheeler was formerly at the University of Birmingham, and is now at Flexible Manufacturing Co., Ltd., Alvechurch, Birmingham. Dr. Kondic and Dr. Ko are lecturers in the Departments of Industrial Metallurgy and of Metallurgy, respectively, at the University of Birmingham.

Table I—ANALYSES OF STEELS USED

Steel	C, %	Ni, %	Cr, %	Mo, %	Mn, %	Si, %	S, %	P, %	N, %	O, %
A	0·24	4·18	1·30	0·68	0·55	0·27	0·032	0·023	0·011	0·011
B	0·32	3·38	0·65	0·26	0·57	0·22	0·016	0·016	0·010	*
C	0·31	3·40	1·26	0·47	0·44	0·20	0·033	0·036	0·011	0·014
E	0·27	3·80	1·70	0·50	0·36	0·20	0·034	0·036	0·005	0·009
VA	0·33	2·59	0·53	0·58	0·68	0·20	0·039	0·029	0·012	0·009
VB	0·32	2·60	0·51	0·55	0·63	0·22	0·028	0·023	0·009	*

* Not determined

steels; both contained less than 0·001% each of oxygen and moisture. The mechanical properties of specimens of the same steel, heat-treated at the same temperature but in different atmosphere, showed a small difference (the cause of which is being separately investigated); this difference, however, does not affect the detailed comparison of specimens of the same steel, nor the general trend between different series.

The specimens for heat-treatment consisted of taper-ended tensile specimens machined longitudinally from the rolled bars. These were 2·5 in. long, 0·252 in. diameter, and had a gauge length of 1·0 in. For heat-treatment, each specimen was suspended on a wolfram wire (insulated from the specimen by an alumina sleeve) from the pulley *E*, and so could be raised or lowered to predetermined positions in the furnace tube *T*.

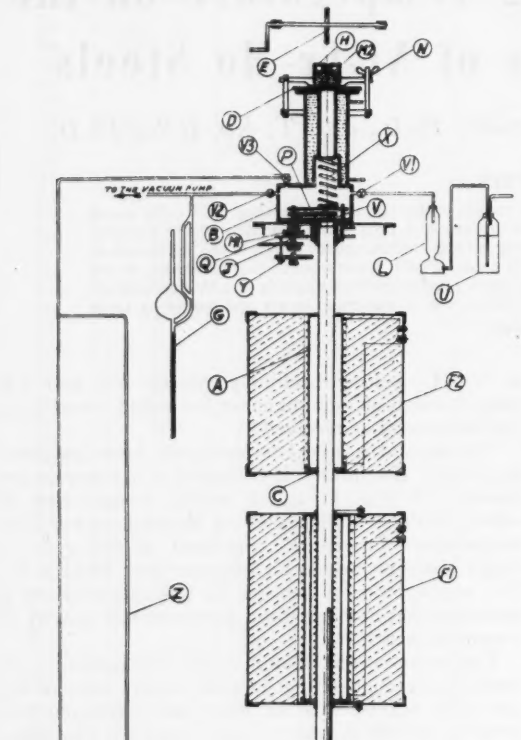


Fig. 1—Furnace equipment

Unless specially stated, the heat-treatment of each specimen consisted of a period of 15 minutes in the low-temperature furnace followed by 5 minutes in the high-temperature furnace; the specimen was then returned for a further 5 minutes to the low-temperature furnace, to attain a constant pre-quenching temperature, and was finally cooled by being transferred to the water-cooled air chamber for 20 minutes (which hereafter will be referred to as air-quenching).

When a specimen was first admitted to the high-temperature furnace the recorded temperature fell quickly by about 50° C., but recovery was rapid and temperature equilibrium was virtually established within the 5-minute treatment period. The temperature immediately before air-quenching, correct to the nearest 5° C., was recorded as the heat-treatment temperature of the specimen. The rate of cooling attained in the water-cooled chamber (*see* Fig. 2) was sufficient to harden each steel to a martensitic structure when the austenitizing temperature was 855° C., although for steels VA and VB the tensile strength was lower than that of oil-quenched specimens.

Mechanical testing was carried out on an Avery tensile testing machine, the gauge length and diameter of the specimens, before and after testing, being measured with a travelling microscope. Vickers hardness measurements were made on sections cut from the mechanical test pieces.

EXPERIMENTAL RESULTS

The results of the investigation are given in Figs. 3 and 4, in which mechanical properties are plotted as a function of initial heat-treating temperature. These results will be considered in more detail, first by examining the figures for each steel in turn, and then from the standpoint of the general conclusions which can be drawn.

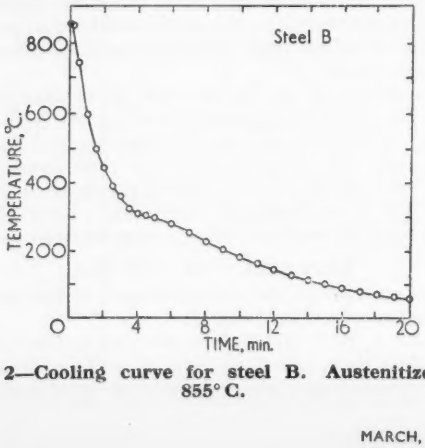


Fig. 2—Cooling curve for steel B. Austenitized at 855° C.

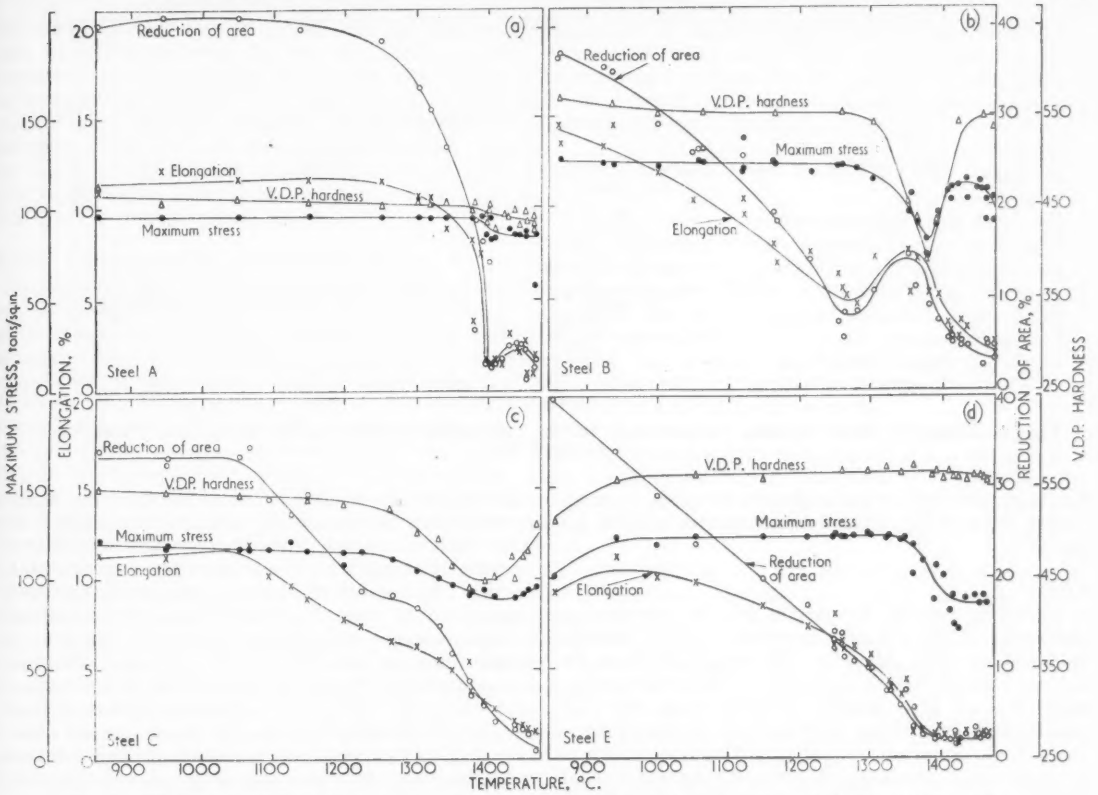


Fig. 3—Effect of initial heating temperature on the mechanical properties of steels. (*a*) Steel A ; (*b*) steel B ; (*c*) steel C ; (*d*) steel E

Steel E

The percentage elongation and reduction of area (Fig. 3*d*) decreased continuously over most of the temperature range studied, and the tensile strength remained constant. Below 950° C., however, there were deviations from this general rule, owing to effects associated with the solution of carbides. Also, above 1350° C. there was a marked drop in tensile strength ; this was found to be associated with a microstructural feature revealed with a sodium cyanide etch. This structure consisted of a mesh of fine dark lines superposed on the normal martensitic structure (*see* Fig. 5*a*). Apart from this feature, which will be discussed later, the structures obtained with steel E were of normal martensitic type, except that a very small amount of feathery bainite appeared in specimens heat-treated above 1395° C., as shown in Fig. 5*b*.

Steels B and C

Mechanical test results on these two steels (Figs. 3*b* and *c*) showed very similar features, and will therefore be described jointly.

Over the greater part of the temperature range studied, the tensile strength remained constant but the elongation and reduction of area decreased progressively with increasing austenitizing temperature.

Within a certain temperature range, different for the two steels, this general tendency was reversed ; the tensile strength and hardness decreased, and the ductility increased, with increasing temperature. This effect was more prominent for steel B, as can be seen from Fig. 3*b* ; the tensile strength and hardness began to drop at 1300° C. and reached a minimum at 1380° C. In steel C the effect was not sufficient to counteract the general decrease in ductility with temperature, and therefore there were no maxima or minima in the properties. Recovery, which was not complete with steel C, commenced at temperatures higher than 1380° C.

Normal martensitic microstructures were found in all specimens of steels B and C, except in the temperature region of the anomalous change in mechanical properties just discussed. When these changes of mechanical properties occurred, the microstructure changed also, and a dark-etching acicular constituent appeared in a white-etching background, as shown in Fig. 6. Etching with picric acid in alcohol further improved the contrast of this microstructure. By microhardness indentations it was found that the dark-etching areas were very appreciably softer than the light-etching background. They occurred in plates which showed some crystallographic regularity, and at higher magnification closely resembled the bainite formed isothermally at about 350° C. in this type of steel.[2] The extent to which the microstructure occurred was proportional to the abnormality in tensile strengths in the same temperature region, and

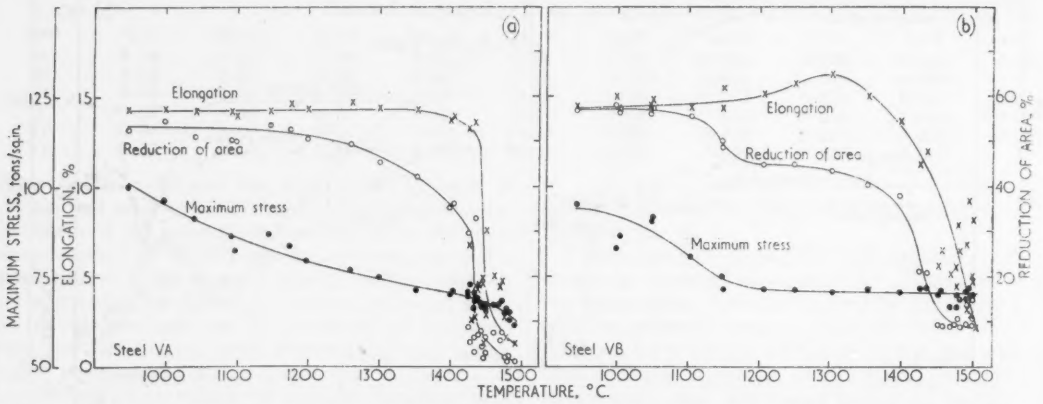


Fig. 4—Effect of initial heating temperature on the mechanical properties of steels. (*a*) Steel VA ; (*b*) steel VB

the structure had not completely reverted to martensite even at the highest temperatures studied for steel C.

Steel A

In many respects the curves for the mechanical properties of steel A as a function of initial heating temperature (Fig. 3*a*) were different from those for the three steels already described. This was particularly true of the ductility curves, which did not exhibit the usual linear decrease with temperatures ; instead, the values remained almost unchanged up to a temperature of about 1250° C., this being followed by an extremely rapid decrease in the range 1250–1400° C., and a slight recovery above 1400° C.

Both the tensile strength and the hardness of steel A were virtually constant up to nearly 1400° C. At this temperature, however, a sudden small, but quite definite, drop in the tensile strength occurred, the reduction being from approximately 95 to 85 tons/sq. in.

The structure of steel A remained mainly martensitic below 1380° C., although traces of bainite were observed above 1300° C. An appreciable amount of bainite was present in the specimens heat-treated above 1400° C. The grain size of this steel increased more slowly in the range 950–1300° C. than the grain size of steel B.[2] Grain sizes were : A.S.T.M. Nos. 8, 5, 4, 3, and 1, at 945°, 1145°, 1305°, 1380°, and 1405° C., respectively, for steel A ; and Nos. 5½ and 2 at 1162° and 1305° C., respectively, for steel B.

Steels VA and VB

The tensile strength of steel VA decreased continuously with increasing temperature, and that of steel VB decreased up to about 1150° C. and then remained constant (Figs. 4*a* and *b*). The hardnesses of the steels, given in Table II, showed a difference of up to 60 points between places near the surface and in the centre of the specimens ; apart from this feature the hardness values changed with initial heating temperature in very much the same way as the tensile strengths.

The reduction of area and elongation of steel VA remained practically constant up to 1250° C. ; above this temperature the reduction of area showed a slight decrease, but the elongation was virtually unaffected until 1425° C. Above 1425° C. very brittle fractures occurred, causing a large scatter of the experimental results. In steel VB the ductility properties remained constant until 1100° C., above which the reduction of area showed a small abrupt decrease. Again, very brittle fractures and wide scatter of results occurred above 1425° C. Figure 4*b* shows that in the region of 1300° C. the elongation and the reduction of area in steel VB varied in the opposite sense, a result quite contrary to that generally obtained throughout this investigation. This was caused by an alteration in the deformation characteristics of the steel, which changed the manner of necking of the specimens.

Microscopic examination of specimens of steels VA and VB showed that the decrease in tensile strength and in hardness was accompanied by a progressive increase in the amount of lower bainite and (with higher initial heating temperatures) bainite present in the structure (*see* Figs. 7*a* and *b*).

Loss of Ductility with Increasing Austenitizing Temperature

From the experimental results, the general conclusion can be drawn that the ductility of quenched specimens decreases as the austenitizing temperature increases. This effect was very marked in steels B, C, and E, but was masked in steels VA and VB by the presence of an increasing amount of the softer, more ductile bainite in the structure as the initial temperature was increased. It is important to note

Table II

VICKERS HARDNESS OF STEELS VA AND VB

Load : 30 kg.

Temperature, °C.	Steel VA		Steel VB	
	Surface	Centre	Surface	Centre
870	491	489	436	430
950	492	481		
1050	466	452	447	442
1150	443	421	379	367
1200	404	341	373	364
1250	396	384		
1350	367	361		
1400	368	356	375	373
1450	362	349	373	365
1480	356	342	372	358

that results of similar work carried out with quenched plain carbon steels are in line with the above general conclusion of continuous loss of ductility with increasing austenitizing temperature.[3] This loss of ductility of martensitic steel is associated with the increase in austenitic grain size. In steel A, it is probable that the lower carbon content and the slowness in grain growth between 1150° and 1300° C. reduced the tendency to embrittlement below 1250° C.

Higher Temperature Brittleness

When the initial heat-treatment temperature was very high—generally above 1400° C.—specimens of all steels became very brittle, irrespective of whether an appreciable loss of ductility had occurred at lower temperature. This effect was also independent of the structure of the specimens tested, since it occurred in steels VA and VB, where the specimens showing this remarkable brittleness were mainly bainitic. Further confirmation was given by a series of tests made on specimens of steel C, which were not air-quenched as in the normal heat-treatment procedure described earlier, but were allowed to cool slowly in the low-temperature furnace. The results of these experiments are given in Table III, which shows that, although the structure of the specimens was that of upper bainite (as was indicated by microscopic examination as well as by the tensile strength), the characteristic brittleness at temperatures above 1380° C. was still present.

It is certain that this phenomenon is closely associated with the severe overheating and burning of the steels at the elevated heat-treatment temperature involved. The temperatures above which overheating and burning of the steels were observed are given in Table IV. These temperatures were determined on quenched and tempered specimens with nitrosulphuric acid, which gives a black network in overheated steels, and with nital (or picral), which shows a white-etching (black-etching) network of phosphorus segregation with iron-rich sulphide inclusions along the austenite grain boundaries, in burnt steels. Sudden embrittlement of the steels VA, VB, A, and B began just below the burning temperature.

The results of the investigations of all these steels are in line with the views on overheating and burning recently advanced by Ko and Hanson,[3] who have shown that overheating is caused by the precipitation of sulphide inclusions on the austenite grain interfaces, and burning by the formation of liquid at the grain boundaries at high temperatures. They have also shown that, when these effects occur, brittle tensile fractures are obtained with very low ductilities. There is no doubt that a similar mechanism was operating in the present steels.

Bainite Formation After Heating to High Temperatures

The third general conclusion arising from this work is that high-temperature austenitizing tends to promote the formation of higher transformation products than martensite. Steels VA and VB, particularly, exhibited this effect, because the cooling rate used in the experiments was only just greater than that required to produce martensite, and in these circumstances small changes in conditions could cause substantial alterations in structure.

More interesting than these two steels, however, are steels B and C, in which the formation of bainite was favoured as the temperature of initial heat-treatment was raised above 1250° C., this tendency being reversed on proceeding to still higher temperatures.

Further evidence that the changes in microstructure and mechanical properties of steels B and C in the temperature range above 1250° C. are caused by the formation of an intermediate transformation product, has been obtained from an experiment in which specimens of steel B were heated to 1370° C. (well within the region of abnormally high ductility and low hardness) and were oil-quenched instead of being air-quenched in the water-cooled air chamber, as is normally carried out. These specimens were fully martensitic, and mechanical tests gave results which (within the limits of experimental scatter) lay on curves that could be drawn smoothly through the entire temperature range, ignoring the maxima and minima produced by the presence of the abnormal bainitic structure.

In steels VA and VB it was possible to suppress the formation of bainite, in specimens heat-treated in the higher temperature range, by increasing the cooling rate ; oil-quenched specimens of steel VB were martensitic whatever the temperature of the initial heat-treatment (although only a limited number of such temperatures was investigated). Mechanical tests showed that, whereas tensile strength was virtually unaffected by increasing initial heat-treatment temperature, the ductility properties decreased steadily, as for steels B and C.

Chemical analyses of specimens of steels B, E, and VB showed no evidence of changes in chemical composition due to the heat-treatment (*see* Table V).

Table III

MECHANICAL PROPERTIES OF STEEL C IN THE FURNACE-COOLED CONDITION

Initial Heating Temperature, °C.	Tensile Strength, tons/sq. in.	Elongation, % on 1 in.	Reduction in Area, %
1020	89	13	42
1110	79	14	42
1200	78	16	38
1300	79	13	25
1350	78	10	14
1380	76	6	9
1410	75	4	6
1425	75	4	6
1450	75	5	9
1460	76	5	7

Table IV

OVERHEATING AND BURNING TEMPERATURES

Steel	Overheating Temperature, °C.	Burning Temperature, °C.
A	1340	1410
B	1300	1400
C	1350	1410
E	1325	1400
VA	1430	1440
VB	1400	1430

Fine Precipitates in Steel E

Further experimental evidence of the nature of the mesh microstructure already reported in specimens of steel E heat-treated above 1350° C., will now be discussed. The development of this structure, which was best revealed, without repolishing, by etching electrolytically in a 10% aqueous solution of sodium cyanide, reached a maximum at 1425° C., and then decreased progressively to the highest temperature studied. The observed cross-hatched lines were found to consist of a series of pits, formed by the removal of a fine constituent which could not be seen in polished sections but was revealed by alkaline sodium picrate etch as very fine inclusions (*see* Fig. 8). The structure resembled closely the fine sulphide dispersions in an Fe–C–Mn–S alloy and in three mild steels heated above 1350° and 1400° C., previously reported by Ko and Hanson.[3]

There is no doubt that these strings were fine inclusions and not residual or precipitated carbide, because (i) they were produced only in specimens heat-treated above 1350° C. ; (ii) the sodium cyanide etch stained but did not remove carbide ; and (iii) the structure persisted after heating a specimen in which it had been previously developed for 30 minutes at 950° C., a soaking found sufficient to dissolve all the carbide in the original steel.

These fine dispersions can be stained with a 1% solution of picric acid in alcohol, and also with boiling alkaline sodium picrate. The normal Whiteley's gelatine–tartar emetic reagent was too weak to attack the inclusions, and therefore a modified reagent was prepared, as follows : 5 g. of gelatine were first soaked in a mixture of 15 c.c. of glycerine and 20 c.c. of water, and the mixture was warmed to a homogeneous liquid. After adding 2 c.c. of sulphuric acid (sp. gr. 1·84), 1 c.c. of aqueous solution of silver nitrate (0·2 g.) was slowly stirred in. Silver nitrate, which gives a black sulphide, was used instead of the antimony salt, to obtain a clearer indication. When a specimen of steel E containing the fine dispersion was coated with a thin layer of this reagent (20–25° C.), the inclusions were attacked and a pattern of black spots was obtained (*see* Fig. 9*a*) which was identical with that produced by etching the specimen with a strong acid, such as 2% sulphuric acid. The pattern was formed in the gelatine, not on the specimen, as it could be removed with the gelatine (Fig. 9*b*). The pattern was not obtained when the gelatine reagent contained only sulphuric acid or sulphuric acid and nitrate ions, but was produced when silver nitrate was added. These experiments showed conclusively that the fine inclusions were sulphide and were, in fact, identical with those observed in the Fe–C–Mn–S alloy mentioned above.

The presence of these fine dispersions in steel E was not affected by the rate of cooling of the specimens from 855° C. When a specimen was slowly cooled (at a rate of about 10° C./min.) from the higher temperature to 855° C. and was then quenched normally, few traces of the dispersions could be found. This suggests that they were formed by precipitation in the austenitic range, rather than otherwise. It is clear, also, that they must have been precipitated during cooling, because their orientation was always constant within a particular austenite grain ; this indicated that they must have been formed after the final austenitic grain size had been established.

The precipitation of sulphide inclusions during cooling through the austenite temperature range is consistent with the suggestion[3] that at high temperature sulphur can be taken into solution in austenite from non-metallic inclusions, but that this solubility decreases markedly during cooling through the austenite range. Precipitation of sulphide will then take place at the grain interfaces, twin boundaries, residual sulphide inclusions, and on favourable planes of austenite when the steel is cooled from high temperatures.

The crystallographic planes on which the precipitation takes place have been identified from the traces of the inclusions in twinned austenite grains.* In Fig. 9*c*, for example, it is possible to identify two trace directions in the parent grain and three in the twin, and the twin boundaries are also clearly identified. On analysis, the planes of precipitation of the sulphide inclusions are found to be the cubic planes {100}.

The precipitation of these fine inclusions on certain crystallographic planes in the austenite accounts for differences observed in the type of fracture obtained on tensile-testing steel E. All the other steels ruptured with a normal cup-and-cone fracture when ductile, and with a brittle fracture, which was coarse for severely overheated and burnt specimens, when the ductility was low. In steel E, however, when austenitizing temperatures greater than 1350° C. were used, practically every austenite grain can be seen to have fractured, macroscopically speaking, across a single plane, these planes deviating by only a small angle from the normal to the tension axis. The type of fracture obtained is shown stereoscopically in Fig. 10*a*. The relation between this type of fracture and the fine precipitate is shown in Fig. 9*d*, from which it is clear

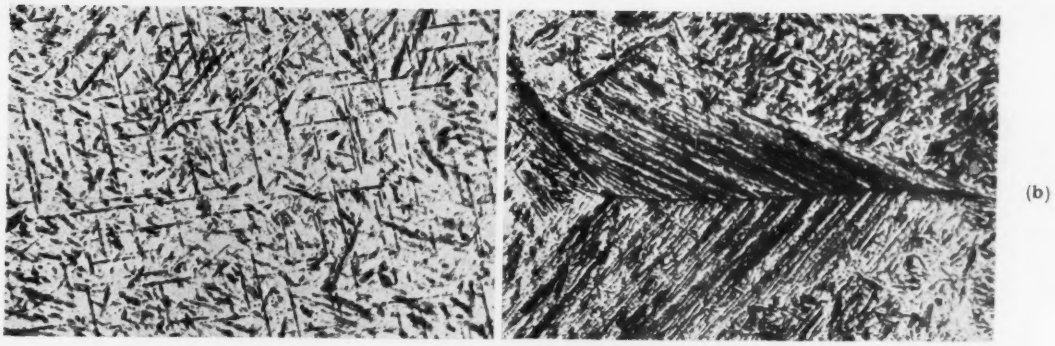
Table V

CHEMICAL ANALYSES OF STEELS AFTER HEAT-TREATMENT

Steel	Austenitizing Temperature, °C.	Analyses C, %	Si, %	Mn, %	S, %
B	1066	0·32	0·22	0·57	0·018
	1215	0·31	0·21	0·56	0·019
	1282	0·32	0·21	0·56	0·016
	1387	0·32	0·22	0·57	0·017
	1475	0·32	0·22	0·56	0·016
E	1053	0·27	0·20	0·34	0·033
	1210	0·27	0·20	0·34	0·032
	1300	0·27	0·19	0·34	0·031
	1360	0·27	0·20	0·34	0·032
	1448	0·27	0·20	0·34	0·033
VB	849	0·32	0·17	0·62	
	1148	0·32	0·17	0·62	
	1302	0·32	0·18	0·62	
	1424	0·32	0·18	0·62	
	1480	0·32	0·17	0·62	

* The authors are indebted to Dr. W. H. Hall, of the Metallurgy Department, Birmingham University, who carried out the identification of the planes of precipitation.

Fig. 5—Microstructures of steel E : (*a*) heated to 1375° C., etched with 10% NaCN × 400 ; (*b*) heated to 1395° C., etched with nital, showing bainite × 500



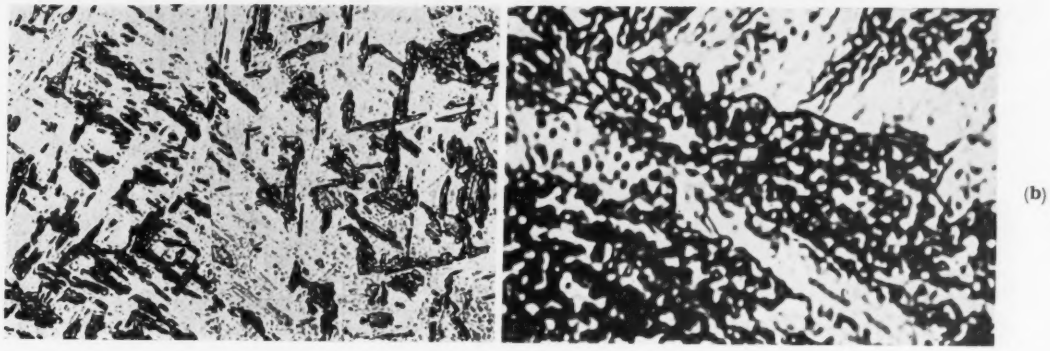


Fig. 6—Microstructures of steel B, heated to 1393° C., etched with alcoholic solution of 1% picric acid and 5 per cent. HCl. (*a*) × 400 ; (*b*) × 3000

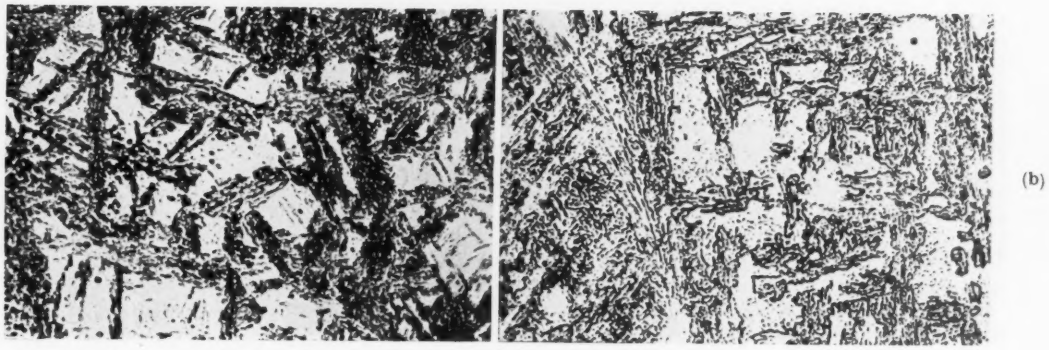


Fig. 7—Microstructures of steel VB, etched with nital × 500. (*a*) Heated to 1250° C. ; (*b*) heated to 1450° C.

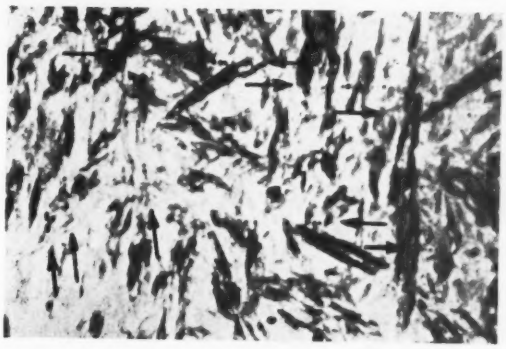


Fig. 8—Microstructure of steel E heated to 1395° C., etched with alkaline sodium picrate × 1500

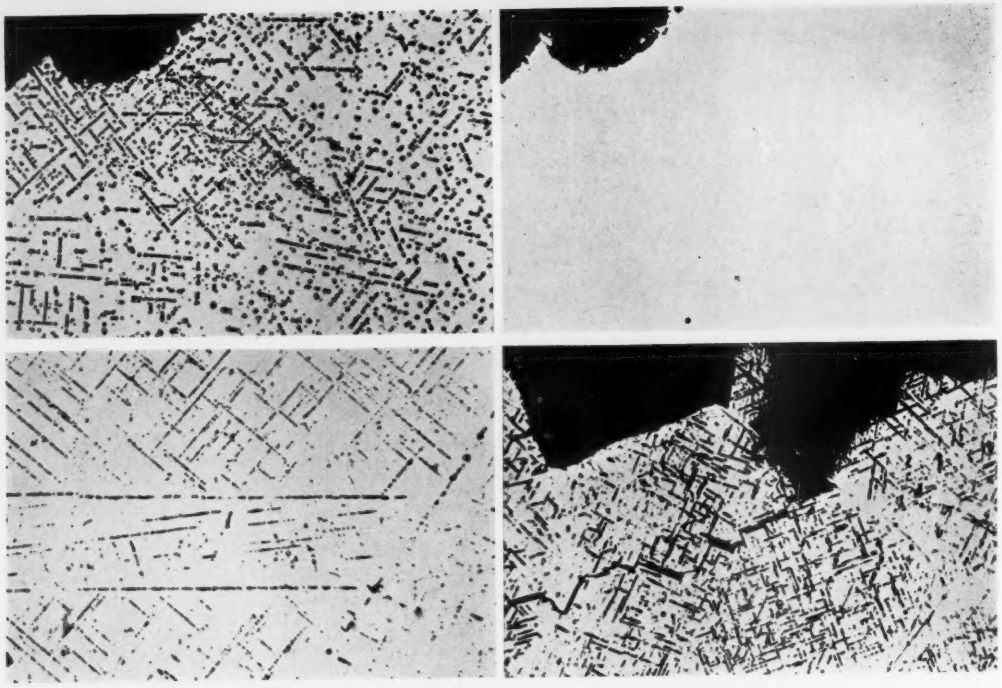


Fig. 9—Microstructures of steel E : (*a*) heated to 1395° C., treated with gelatine/silver nitrate reagent × 400 ; (*b*) as in (*a*), but gelatine film removed ; (*c*) heated to 1425° C., etched with 2% sulphuric acid × 400 ; (*d*) heated to 1420° C., etched with 2% sulphuric acid × 175

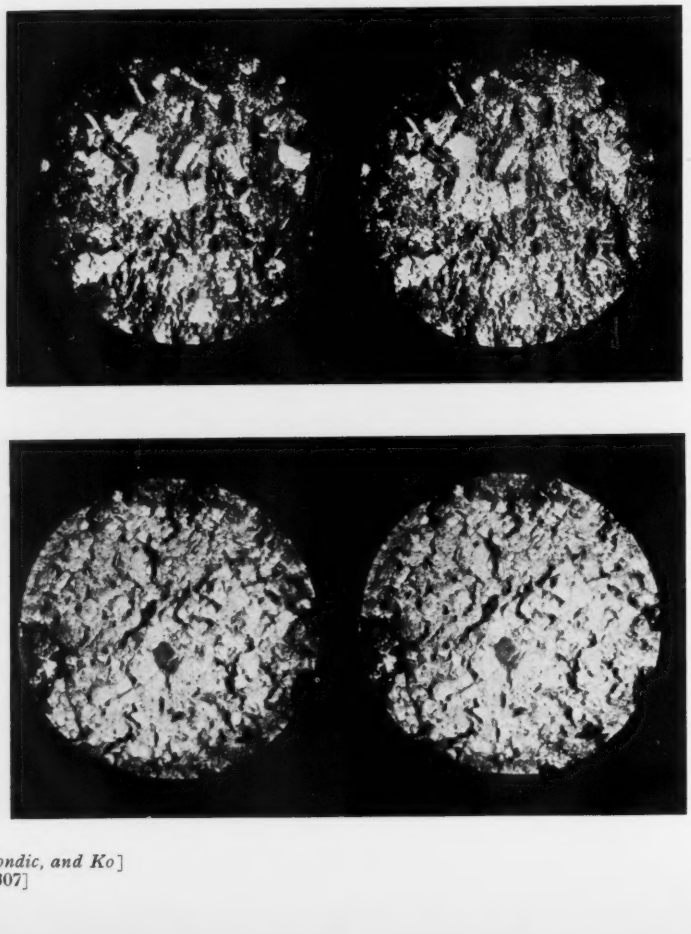


Fig. 10—Stereoscopic views of fracture of steel E × 7. (*a*) Heated to 1425° C. ; (*b*) heated to 1460° C., slowly cooled to 855° C., air-quenched

that the presence of numerous fine inclusions precipitated on particular crystallographic planes favoured the propagation of cracks along these planes, producing cleavage fractures.

The marked drop in tensile strength of steel E when the initial austenitizing temperature reached 1350° C. is attributed to the presence of these precipitated inclusions, a conclusion which has been confirmed by testing a specimen slowly cooled from 1460° to 855° C. before normal quenching. In this specimen very little planar precipitation had occurred, and the normal brittle fracture shown stereoscopically in Fig. 10*b* was obtained, instead of a faceted cleavage fracture. The ultimate tensile strength of this specimen was 102 tons/sq. in., a value which, although not quite equal to the tensile strength which would have been obtained with an austenitizing temperature below 1350° C., was very much greater than would have been obtained from 1460° C. if Widmanstätten precipitation of the sulphide inclusions had been allowed to occur normally.

DISCUSSION

Two interesting results require further experimental investigation. The first is the observation that the hardenability of the Ni–Cr–Mo steels investigated can be reduced by high-temperature heating. As has already been shown, this reduction of hardenability was not caused by changes in the normal chemical analyses of the steels. Two possible mechanisms by which the formation of an intermediate transformation product, rather than martensite, might be favoured by high-temperature heating are as follows :

(i) The local modification of the transformation characteristics of the steel, by composition fluctuations produced by the occurrence of the γ–δ transformation during the high-temperature treatment. The existence of a two-phase region will certainly cause the segregation of certain elements such as C, Ni, and Mn, which are less soluble in ferrite than in austenite.

This theory is attractive in that it might be made to account for (*a*) the crystallographic regularity of the bainite structure illustrated in Fig. 6, if it were assumed that the γ–δ transformation, the consequent composition fluctuations, and hence the occurrence of the intermediate transformation on cooling, took place upon crystallographic planes in the γ-phase, in the same way that the γ–α change occurs on cooling ; and (*b*) the fact that in steel B and C the formation of a bainitic transformation product is reversed on proceeding to still higher temperatures, which might occur on leaving the two-phase $\gamma + \delta$ region.

This theory cannot explain the results on steels VA and VB, in which the tendency to form bainite at the cooling rate employed increased continuously over the whole temperature range. It is also unable to account for the sensitivity of the duplex structure of the type shown in Fig. 6, to the rate of cooling. Oil quenching instead of air quenching produced an entirely normal martensitic structure with no evidence of composition inhomogeneity. The theory also has to assume that there is substantially no change in the austenite grain boundaries before and after the γ–δ transformation, so that a structure related to the final austenite grain boundaries is accounted for on the basis of a change connected with the original austenite grain boundaries.

(ii) An alternative theory is that there is a general modification of the transformation characteristics of the steels, *i.e.*, a shift of the transformation–temperature–time curves towards shorter induction periods for bainitic transformation, as the initial austenitizing temperature is raised.* An effect of this kind has already been reported by Winterton.[4] If such a shift does, in fact, occur, the knee of the transformation curve for continuous cooling might well be intercepted by the cooling curve for the experimental conditions used, giving a transformation to an intermediate product, whereas previously the knee had not been intercepted and martensite had been produced.

Although it was not possible to carry out actual hardenability tests on the steels used in this investigation, calculations based on the compositions of steels[5–7] indicate that steels B, VA, and VB, have the lowest hardenability, and steels A and E the highest values. This is entirely in accordance with experimental results, in that intermediate transformation products have been obtained only in the highest temperature range in steel A and in very minute amount in steel E ; in these steels the T–T–T curve presumably lies so far to the right that the shift produced by the high-temperature treatment is insufficient to cause early intersection of the cooling curve.

The second theory appears the more tenable, although no satisfactory explanation of the postulated shift of the T–T–T curves after heating to high temperatures can be advanced at present. Winterton[4] has suggested that this might be caused by homogenization of the steel, but this seems quite inadequate to account for the present results. Thompson and Stanton[8] have observed that the presence of gases in a steel alters its transformation characteristics. They also found[9] that the rate of decomposition of austenite may be accelerated by burning in air. It is possible that, in the present experiments, the high-temperature heating has caused some changes in the state of the impurities, so affecting the transformation characteristics.

Although, on this basis, an explanation can be made of the tendency to produce higher transformation products when higher initial heating temperatures are used, there is still the question of the reversibility of the effect in steels B and C. It is tentatively suggested that this is due to an increase in the hardenability of the steel, caused by an increase in its manganese content. At the temperatures at which the effect occurred there is abundant evidence that manganese sulphide inclusions had been taken into solution in the austenite, and, on subsequent cooling, the sulphide precipitated is iron-rich. The overall effect of the high-temperature treatment is thus to increase the manganese content of the steel,

* Since this paper was written, investigations carried out by one of the authors (T.K.) and Mr. S. A. Cottrell have confirmed that the isothermal bainitic transformations in steels VA and VB are accelerated by increasing the austenitizing temperature.

and the effect of this upon its transformation characteristics may be sufficient to reverse the opposite tendency, due perhaps to changes in the state of the impurities.

The second interesting result is that the precipitation of the fine inclusions found in specimens of steel E when heat-treated above 1350° C. has not been observed in the other five steels. Neglecting the fine dispersions, this steel appears cleaner than the other steels after the high-temperature treatment. It is very probable that the solubility of sulphur in Steel E, particularly at high temperatures, is higher than that in the others, because of the low Mn/S ratio in this steel. There will be more sulphide to be precipitated during cooling, and less residual sulphide to act as nuclei for precipitation in this steel after the high-temperature heating. Ko and Hanson[3] have suggested, in relation to overheating, that the precipitation of sulphide on austenite grain interface during cooling through the austenite region depends on the austenite grain size, the amount of residual sulphide particles which can act as precipitation nuclei, the rate of cooling through the high-temperature range, and other similar considerations. This will, of course, apply equally to the case under consideration, and it may therefore be assumed that in steel E, with its large sulphur solubility at high temperatures, conditions are such that there is insufficient time for all the sulphur to diffuse to the grain boundaries, and an insufficient number of randomly oriented precipitation nuclei within the grains. The sulphur accordingly precipitates as sulphide on the favoured crystallographic planes of the austenite grains.

CONCLUSIONS

The main conclusions arising from this investigation may be summarized as follows :

(1) Where fully martensitic structures were obtained under the experimental conditions employed, an increase in the austenitizing temperature caused a progressive fall in the percentage elongation and reduction of area of the steels investigated, the ultimate tensile strength remaining constant. To this general conclusion the results on steel A constitute an exception.

(2) Very high initial heat-treatment temperatures result in brittleness of a different type, due to overheating and burning of the steel, this effect being independent of the structure of the specimens tested.

(3) High initial heating temperatures favour the decomposition of austenite to higher transformation products than martensite, but complex compositional changes occurring at the elevated temperatures involved may cause an apparent reversal of this tendency.

(4) A microstructural phenomenon in one of the steels, consisting of a Widsmanstätten pattern of fine dark lines associated with faceted cleavage tensile fractures and a pronounced drop in tensile strength, has been shown to result from the preferential precipitation of sulphide inclusions on {100} austenite planes.

Acknowledgments

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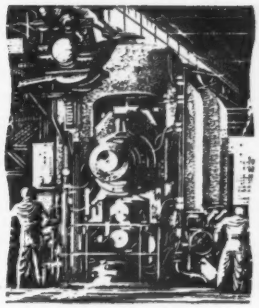

Electric Drives for Reversing Hot Mills

By P. E. Peck, B.Sc.(Eng.), A.M.I.E.E.

SYNOPSIS

Consideration is given to the power demands made by the rolling process and to the response which must be expected from the drive. The electrical and mechanical characteristics of the main-drive motor are described before dealing with the drive itself. Single- and twin-motor drives are compared from various aspects. The advantages of the twin-motor drive are stated.

The factors governing the design of the main-motor generator set are explained and systems for lubrication and ventilation are described.

The requirements and capabilities of the control scheme are summarized and a description is given of one form of modern control scheme using rotating regulators.

The paper concludes with a reference to the performance which can be expected from the modern mill.

Introduction

THE reversing hot mill, as a blooming or slabbing mill, is the first link in the chain of every modern rolling-mill plant of any size and is the basis of most section and plate mills. It also plays a considerable part in the hot-rolling of aluminium, light alloys, and other types of alloy sheet. With so wide a variety of products there are bound to be notable differences in the arrangement of such mills as well as differences in the manner in which they are handled, even although the main essentials of the unit remain the same.

Differences in mill layout and in the auxiliary equipment used in handling the metal will not be considered here, but before dealing with drives themselves it is desirable to assess the physical requirements of such mills, both as regards the demands for power and speed and also control characteristics and methods of handling.

PHYSICAL PROCESS OF ROLLING

Power Requirements

The power required by a mill can be determined by considering (*a*) the maximum torque which is needed to bring about the required reduction in the metal, and (*b*) the highest speed at which this peak torque is required. Once the metal has entered the rolls the rolling torque remains substantially constant irrespective of the speed of rolling, and the drive must therefore have a constant torque characteristic up to the highest speed at which the maximum torque is required. It is upon this basic speed–torque requirement that the size of the mill motor, the capacity of the motor generator set supplying it, and in consequence, the major part of the capital cost of the drive, depend.

For a given speed of rolling or a given tonnage output, the power required for forming the metal can be supplied by a large-diameter roll revolving at a relatively low speed or by a smaller-diameter roll revolving at a higher speed. As the size of a motor is more or less inversely proportional to the speed at which its power output is required (or, more accurately, its size is approx. directly proportional to the torque it has to produce) it is obviously advantageous to use a diameter of roll as small as possible yet compatible with the required mechanical strength, although other factors, such as the size of ingot and intended drafting, must also be considered. The later passes of any rolling schedule are of increasing length and it becomes desirable to make use of higher speeds to shorten the time taken and to avoid undue cooling of the metal. Fortunately, in later passes smaller reductions are made (even although percentage reductions may be a little greater) so that these higher speeds are only required at reduced torques, and the power demand is not increased. Summarizing, the drive for a mill must be able to supply constant torque up to a certain speed, which is referred to as base speed, and constant power up to the maximum speed of rolling, referred to as full speed. This is shown graphically in Fig. 1 where (*a*) is the curve of torque against speed, and (*b*) is the curve of power

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Mr. Peck is in the Industrial Engineering Department of the British Thomson-Houston Co., Ltd., Rugby.

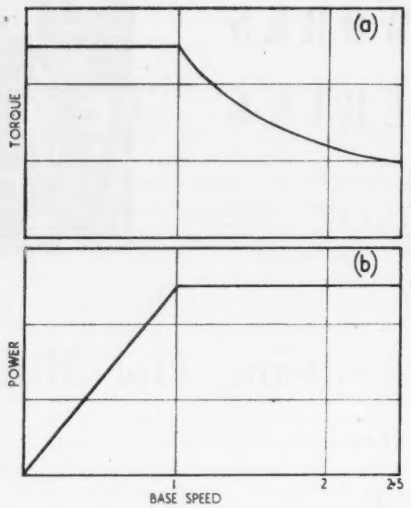


Fig. 1—Torque and power requirements of reversing hot mill

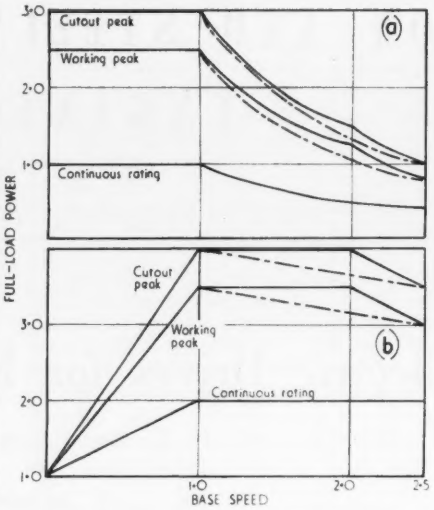


Fig. 2—Rating curves for reversing hot-mill motor

against speed. These curves will be considered more fully later in dealing with the machines themselves.

Methods of Handling

The way in which the mill is handled varies with the application. For instance, with a slabbing or plate mill, entering the metal (except perhaps for an edging pass in a slabbing mill) involves no difficulties, and on such mills the metal is usually entered at base speed, accelerated, if the drafting permits or length of material warrants it, and ejected (all too often at high speed), after which the mill is reversed immediately, ready for re-entering as soon as the screwdowns have been adjusted. Where quick operation of the screws is possible, time will often be saved by retarding the mill before the metal leaves the rolls. This also reduces the distance the metal travels away from the rolls and back again.

Clearly the main requirement in the above case is that the times of acceleration, retardation, and reversal should be as short as possible.

In the case of a blooming or section mill, however, much more care has to be taken in entering the metal as the section must be threaded into its pass. Entering is always at a low speed and it is desirable for the driver to have good response, *i.e.*, a close feel and quick following of the controller movements, so that it is possible to check quickly if a false entry is made. One obvious difference in operation is that in the case of the section mill much more of the acceleration takes place with metal between the rolls than in the case of the plate mill.

The degree of torque reduction permissible in the constant-power range of the drive is dependent upon a number of factors. As regards the material itself, the degree to which the metal hardens with cooling may in certain cases render a marked reduction of torque impossible, or again certain materials cannot readily be rolled at high speeds without danger of splitting. On the other hand if a wide range of products is envisaged (for example, different widths of plate) the smaller torques required for a narrow plate may make much higher rolling speeds practicable and consequently a high degree of torque reduction with consequent speed increase is desirable. Probably more consideration than at present could be given to these factors in deciding the rating of the mill motor.

With these points in mind the different aspects and problems which present themselves to the designer of the electric drive may be considered.

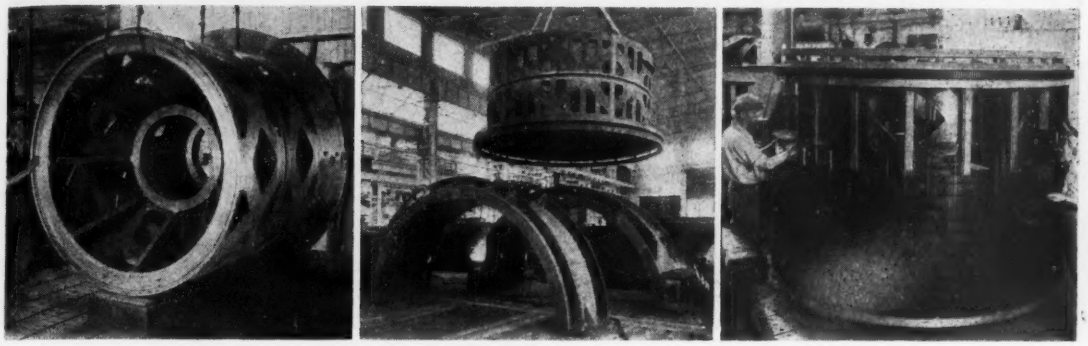
THE REVERSING-MILL MOTOR

Electrical Considerations

The full-load rating of an electrical machine is that power which it is able to exert continuously without exceeding the prescribed temperature rise. A reversing-mill drive is rarely run at full load ; the load is a fluctuating one of short peaks interspersed by short intervals of light or no load. Providing the r.m.s. value of the load over a given period does not exceed the full-load rating of the machines they will not overheat. It might seem, therefore, that provided these peaks are short enough, there is no limit to their value if the r.m.s. rating is not exceeded. There is, however, another limiting factor, namely, the ability of the machines to commutate high peak current and particularly peaks suddenly applied or removed as in the reversing mill.

The relationship between the operating peak and the continuous rating of a machine, and the bearing which each has on the size of the machine, is not easily assessed, but in simple terms it may be stated that for a forced ventilated motor the maximum permissible electrical loading under peak conditions will result in a machine which is capable of a continuous 50° C. temperature rise rating of about 40% of this peak.

Therefore, when the r.m.s. loading required is less than 40% of the peak, the size of the machine will be



(a) (b) (c)

Fig. 3—Fabricated armature spider (*a*) for 108-in. dia. motor, (*b*) for 144-in. dia. machine, (*c*) showing assembly of the core

dictated by the peak, and heating will present no difficulty. Where the demand is more than 40% of the peak, the size of the machine will be dictated by heating considerations, and it will easily deal with the peaks.

This very general statement is complicated by the fact that no manufacturer produces an infinite range of machine sizes, and the design therefore goes in steps. It may not be felt desirable to allow such high electrical loading for the longest machine used at a certain diameter, because with such a machine the ratio of maximum permissible peak load to maximum permissible continuous load, would be less than the same ratio for a machine using the next larger diameter and a shorter length.

In practice the duty-cycle imposed on a reversing hot-mill motor rarely reaches its continuous rating and the ability to meet the peak loading is generally the governing factor. There are, of course, exceptions to this, particularly where very long passes are taken, and a full programme is being rolled as in the hot-finishing of aluminium or steel strip.

It is the usual practice in this country to rate reversing-mill motors to have an overload capacity of 25% for 2 hr. (a heating limitation) with a working peak torque of 2·5 × full load and a circuit-breaker setting or cutout peak of 3 × full load.

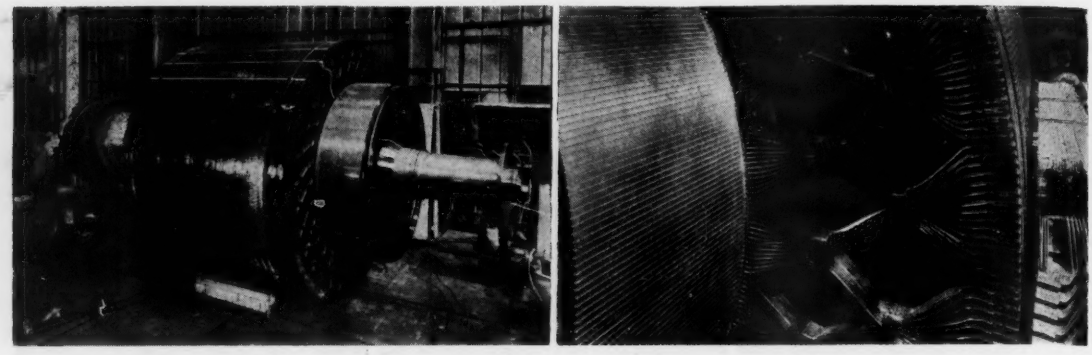
The American practice is to rate such machines with a working peak torque of 2·25 × full load and a circuit-breaker setting of 2·75 × full load. Where an American and British machine are both designed for the same working peak torque therefore, the nominal continuous rating of the British machine is a more accurate picture of the machine's capabilities than that of the American machine. The ratio of peak to continuous rating is dealt with more fully in the section on the motor generator set (p. 316).

The foregoing remarks refer to full-field conditions but the ability to commutate overloads is reduced as the speed of the motor is increased by field weakening. It is not unusual to specify a lower permitted peak at the higher speeds if the field-weakening range exceeds 2 : 1, and although overload-relay or circuit-breaker settings cannot easily be altered in step with motor speed this effect can be overcome to some extent by control circuits of modern design. Methods are also now available for improving commutation at high speeds.

The curves in Fig. 1 may be amplified to give a picture of the capabilities of the machine. In Fig. 2 curves are given for a reversing-mill motor with a working peak torque of 2·5 × full load and a field-weakening range of 2·5 : 1. Curves for both the nominal full-load capacity and the peak capacity of the machine and for the restriction of peaks at higher speeds are shown. The broken lines show how American manufacturers rate their machines for progressively reduced torques in the field-weakening range. In working out the drafting for a rolling schedule the sum of rolling torque and accelerating torque must always be less than the peak torque indicated on the curve.

Mechanical Considerations

The outstanding feature of a reversing-mill motor is its robust construction which is necessary to withstand the repeatedly reversing stresses to which it is subjected and more particularly the impact shock loads imposed upon it when metal enters the rolls. This robust construction must apply, not merely to the mechanical parts of the motor, but also to the electrical connections which must be strongly braced to ensure that repeated shocks do not give rise to fatigue fracture. The steps taken by the design engineers of one electrical manufacturer to meet these requirements may be examined. The armature spider, illustrated in Fig. 3*a*, is of a rigid drum construction; the modified geodetic periphery of the drum provides high torsional rigidity with relatively light weight and with ample access for ventilating air to reach the core cooling ducts. The view in Fig. 3*a* is taken from the drive end; another example for a much larger machine is shown in Fig. 3*b* which also illustrates the two halves of the magnet frame in an early stage of manufacture. In Fig. 3*c* the core is being built up on this spider, which is keyed to the shaft by a number of dowels between a suitable flange on the shaft and the spider, thus distributing the driving stresses evenly all round the



(a) (b)

Fig. 4—Fabricated armature (*a*) completed, (*b*) showing rigid bracing of commutator risers

shaft. The photograph of the completed armature (Fig. 4*a*) shows the relatively great length of the armature in proportion to its diameter which reduces inertia to a minimum. The rigid bracing of the commutator risers, shown in detail in Fig. 4*b*, is an effective method of strengthening and also gives access for cleaning behind the risers, an otherwise inaccessible area.

Figure 5 shows the magnet-frame assembly of the motor ; the rolled-steel fabricated construction widely adopted at present will be noted. Steps are again taken to ensure a robust construction with the compole and compensating windings rigidly braced. In Fig. 5 the method of bracing the commutating poles to the adjacent main poles is also shown. Figure 6 shows a completed machine on site, driving a reversing blooming mill ; this motor has a cutout rating of 13,700 h.p.

THE REVERSING-MILL DRIVE

Comparison of Single- and Twin-Motor Drives

Inertia Factor—As already mentioned, the reversing-mill motor is designed as a long small-diameter machine to reduce the inertia as much as possible. As the size and power of drives increase the inertia increases more than proportionally. For a given ratio of armature length to diameter

$$Wr^2 = k\,\frac{(\text{h.p.})^{4/3}}{(\text{r.p.m.})}$$

where k is a constant, W is the armature weight, and r is the radius of gyration. Figure 7, in which this relationship is plotted for a number of designs, shows that for a single-unit machine, at a given speed, the greater the power the greater is the proportion of the torque capacity required for accelerating the machine. Therefore a point is reached when it is better to use a double-unit machine than a single-unit one and the natural outcome is the double-armature motor. This can be applied through the conventional pinion drive or alternatively the mill pinions may be dispensed with and two duplicate machines used, one to drive the top and the other the bottom mill roll, relying upon electrical means for matching the loads and speeds of the rolls ; this is the so-called twin-motor drive. It has a number of advantages on its own account and might even be used where otherwise a single-unit machine would be adopted.

For the twin-motor drive the mechanical consideration of roll changing makes it practically essential to keep both motors on the same side of the mill. One motor is mounted behind the other, and drives *via* a jack shaft over the top of the bottom motor. The jack shaft is supported in a bearing mounted on a bridge spanning the bottom motor-drive end bearing. The distance between the motor-shaft centres makes it necessary to use longer spindles between the rolls and the driving ends of the motor shafts than in the pinion drive to avoid excessive angularity in these drives. The distance between the centre lines of driving shafts can be further reduced by again breaking down each of the twin-drive units into a double-armature machine ; this permits shorter mill spindles for a given maximum angular displacement but, for a given power of drive, introduces a greater

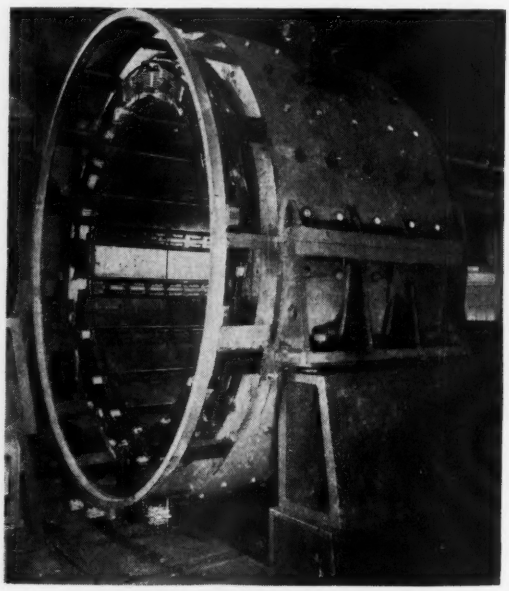


Fig. 5—Magnet-frame assembly

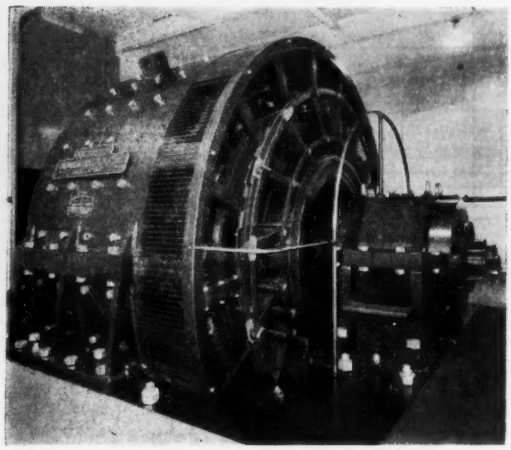


Fig. 6—Reversing blooming-mill motor, 62–160 r.p.m., 13,700-h.p. cutout peak

overall length for the entire set-up which might quite easily involve additional expense in the provision of a larger motor room.

Provision must be made on a twin-motor drive for taking end-thrust caused by friction in the universal couplings on the mill spindles. With a pinion drive this is done in the pinion housing. With a twin drive the drive end bearings of the motors are provided with special babbitted thrust faces engaging with thrust collars on the motor shaft, to take either a push or pull on the shaft. In addition it is usual to mount a special frame for supporting the top spindle carrier bars on the bottom bedplate, although it is sometimes separately mounted.

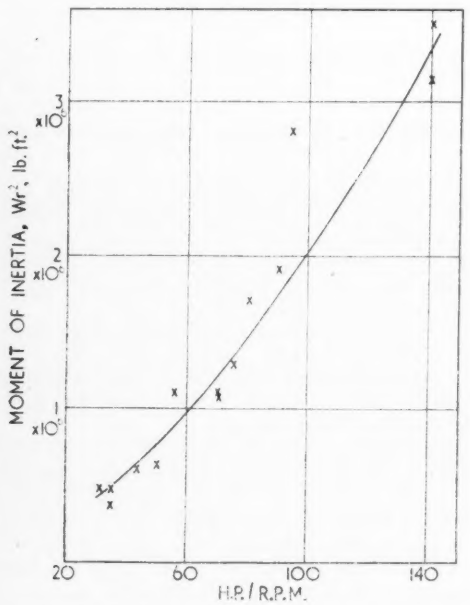


Fig. 7—Relationship between power and inertia of reversing hot-mill motor

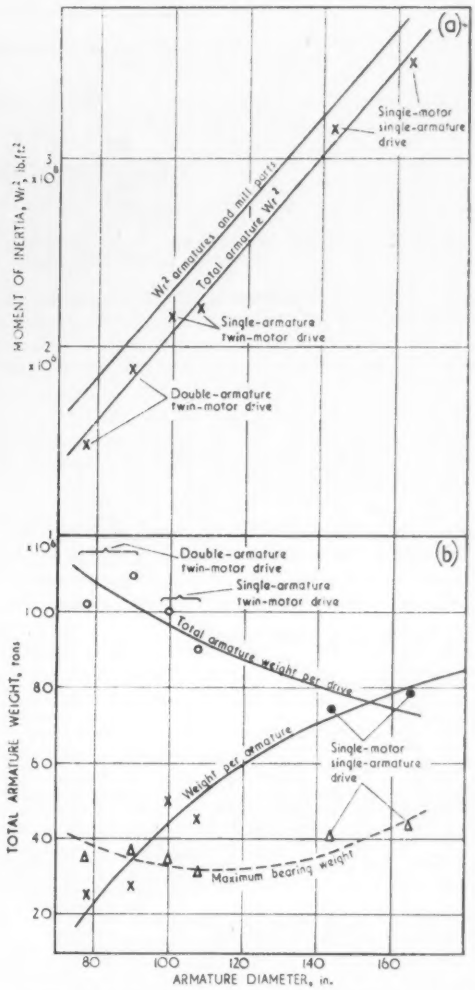
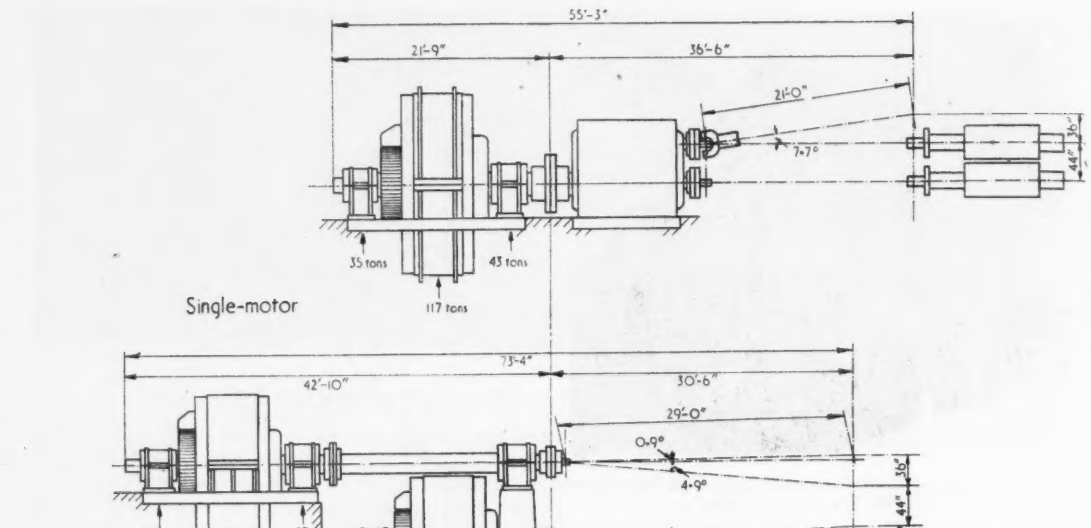


Fig. 8—7000-h.p., 50–120-r.p.m. reversing hot-mill motors: (*a*) total Wr^2/armature diameter, (*b*) total armature weight/armature diameter, for various arrangements

A 44-in. blooming mill driven by a 7000-h.p. 50–120-r.p.m. motor, a size and power which have been very commonly used in America, make a good basis for comparing drives. There is a number of possible designs of drive—a conventional single-motor and pinion drive, a double-armature motor and pinion drive, a single-armature twin-motor drive, or a double-armature twin-motor drive—the first and the last two of these are described in detail, two possible frame sizes for each arrangement being considered.

Figure 8*a* shows that the total inertia of the drive is proportional to the diameter of the machine armature. The upper line which gives the total inertia, including the mill parts, shows that the greater part of the total is contributed by the inertia of the motor. Figure 8*b* shows that this improvement in inertia is not without increased expense in the use of material: the total weight of the armatures in the



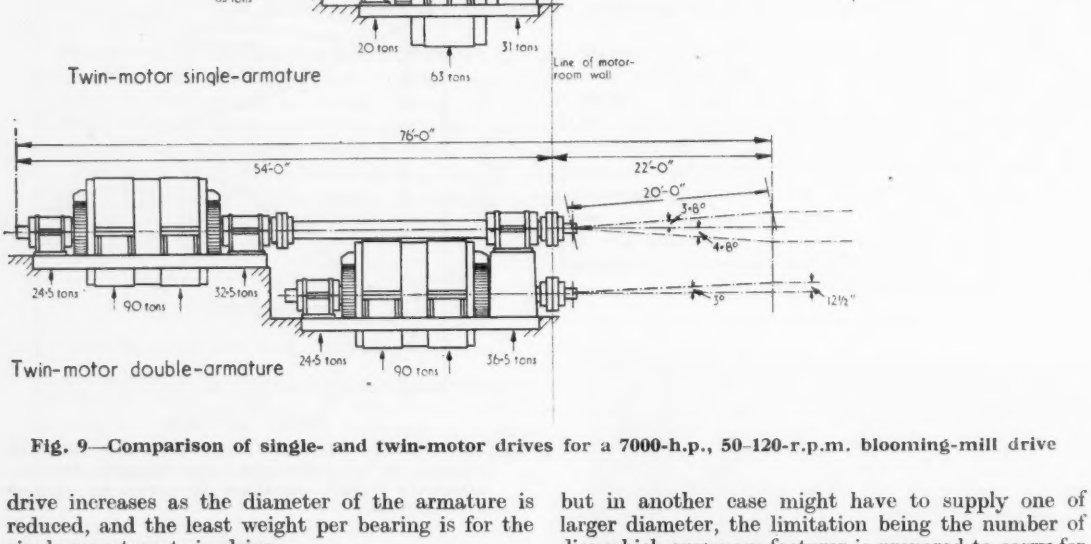


Fig. 9—Comparison of single- and twin-motor drives for a 7000-h.p., 50–120-r.p.m. blooming-mill drive

drive increases as the diameter of the armature is reduced, and the least weight per bearing is for the single-armature twin drive.

Although the power output of a machine is dependent both on the diameter and on the length of the armature, the electrical limitations under peak conditions set a limit to the length that can be adopted for a given diameter, and for this reason alone the designer may have to step-up to the next larger diameter, although from a mechanical viewpoint a longer machine would be quite practicable. In the cases under consideration the larger-diameter machine in each type of drive is preferable electrically, and attention is focused on the single-unit machine with a 165-in. dia. armature, the single-armature twin drive using a 108-in. dia. armature, and the double-armature twin-motor drive using a 90-in. dia. armature.

An electrical manufacturer using different steps in the range of frame sizes might, in one of these cases, be able to produce a smaller-diameter machine but in another case might have to supply one of larger diameter, the limitation being the number of dies which any manufacturer is prepared to carry for producing pole and armature punchings. As machines of this type are not produced on a quantity basis there are obvious limitations.

The three drives under consideration are shown in Fig. 9 driving a 44-in. blooming mill with a 36-in. lift. The three elevations shown indicate the distance which the mill and drive project into the mill and electrical bays respectively.

Mill Spindles—The angularity of the mill spindles shown in Fig. 9 is typical of present-day practice on these mills and for the double-armature twin-motor drive the length of these spindles has been chosen so that the angular deflection is the same as that of the single-armature twin-motor drive. It is not improbable, however, that this will bring the mill too near to the dividing wall between the two bays to accommodate the necessary oil cellars, scale pits, etc., and

therefore the spindles would have to be longer with a corresponding increase in overall length of the entire unit. This would minimize one of the advantages of the double-armature twin drive, namely, the reduced distance between shaft centres, as compared with the single-armature twin drive, with consequent reduction in mill spindle length for a given maximum angular displacement.

There is little published information about mill spindles, but Stover,[1] in America, mentions the necessity for long spindles for the modern slabbing-blooming mill with its high lift for edging passes, and he states that the maximum allowable angle of inclination of mill spindles for these passes where the maximum torque is not expected, is 10°. With a pinion drive this limit of angle may result in spindles up to about 27 ft. The use of a twin-motor drive considerably reduces the maximum angle but may increase the angle at which maximum work is done. Stover gives an example for a 10,000-h.p. 4-high plate mill drive with a maximum angle of 4°, and where 36-ft. long spindles are used.

Investigation of various twin-motor-drive installations shows spindle lengths varying from 25 to 36 ft. and maximum angles up to about 6° for the upper spindle, although this is generally limited to 5° or just below, with a maximum angle of 3° for the lower spindle with the smallest roll diameter used. Generally, the heaviest torques are taken with deflections of 3° or less above or below the horizontal, but angles of 5° and 36-ft. spindle length are not regarded with any great concern.

Long spindles of this type are supported on carrier bars by bearings with suitable spring loading adjusted to relieve the segments of the universal couplings of the weight of the spindle ; these couplings carry only the torque load. The twin-motor drive, therefore, does not involve so great an increase in mill spindle size as might at first be thought.

Comparison of Inertia and Weight—Taking the average of the two inertia figures for each of the three types of drive given in Fig. 8*a* will show that in stepping from a single-motor drive to the twin-motor single-armature drive there is a reduction of 32·5% in the Wr^2 of the drive, whereas in the second step to the double-armature twin-motor drive the reduction in inertia is only a further 13½% of the original.

The weights indicated in Figs. 8*b* and 9 also show that the double-armature twin-drive unit is less economical in material and consequently is likely to be more expensive. The increase in weight will also be reflected in the cost of foundation work.

Comparison of Cost—A comparison of the cost of the different arrangements of drive and the relation of the cost of the drive motor to the remainder of the electrics, *i.e.*, switchgear, flywheel M.G. set, control gear (A.C. and D.C. associated with the mill itself), ventilating equipment, and cables is given in a very general way in Table I.

In the step from a single-motor drive to a twin-motor drive the increased cost of the electrical equipment is considerably offset by the omission of the pinions, but the further step from a single-armature twin drive to a double-armature twin drive could only be justified on the score either that the single-armature arrangement would involve excessive length of mill spindles to maintain reasonable angularity or that a marked improvement in performance would be brought about by the reduced inertia, which, as has been shown, is small.

Table I

APPROXIMATE COMPARISON OF COSTS FOR REVERSING-MILL DRIVE

(7000 h.p., 50–120 r.p.m., peaks 2·5 × full-load working, 3 × full-load cutout)

	%
Single-motor single-armature	100
,, ,, double- ,,	125
Twin-motor single- ,,	140
,, ,, double- ,,	175
Switchgear, M.G. set, control gear, cables, ventilating equipment	135

Probably the cost of the pinions for a drive of this size would be about 35% of that of the single-drive motor. A straight comparison of first cost is hardly justified because the life of the electrics will be appreciably greater than that of the pinions, which will have to be replaced from time to time, and whereas the maintenance of the electrical equipment will be slightly increased in the twin-motor drive, the maintenance of the pinions will be entirely eliminated.

Advantages of the Twin-Motor Drive—The advantages of the twin-motor drive over the conventional pinion drive are summarized by Wright[2] as follows :

(1) The tendency for the rolls to chatter at the beginning of the pass is eliminated, because of reduced backlash and the freedom of the rolls to adjust themselves to the steel

(2) Higher entering speeds can be used and less care is required on entering steel in the rolls. Steel seldom fails to enter on the first trial

(3) Armature inertia is reduced leaving more torque for useful work or permitting faster acceleration, or a measure of both

(4) It is not necessary to match roll diameters exactly. With smooth rolls where pass area is not affected differences in diameter of 5–10% are permissible

(5) The amount of power which may be applied to a single pair of rolls can be increased without resorting to difficult mechanical or electrical designs.

Maintenance of pinions is also eliminated and it is claimed that in the case of plate mills, pinion marking by worn gears is avoided. In a pinion drive there are five places where backlash can develop between the motor and the top roll, and four places between the motor and the bottom roll. In the twin drive there are only two places in each case, both in the universal spindle couplings.

The twin-motor drive is favoured where the power demand is sufficient and in American practice over the last two or three years this form of drive has been used for mills with a total h.p. of 8000, 7000, 6000, and 5000. The most usual speed range is 40–80 or 50–120 r.p.m.

The first American installations had 10,000-h.p. total capacity, and date back to about 1930. Double-armature twin drives were used, but satisfactory spindle behaviour resulted in the adoption of single-armature motors for later installations, and as far as the author is able to ascertain no double-armature

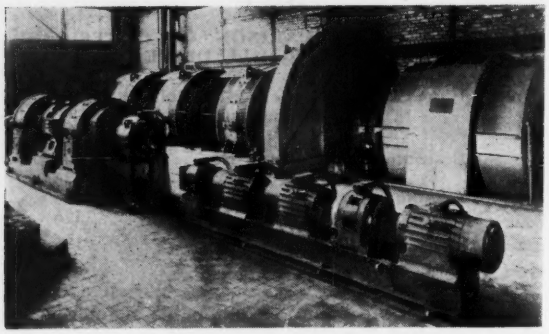


Fig. 10—Main M.G. set, main exciter set, and pilot exciter set for 4850-h.p. reversing structural-mill drive

twin-motor drive has been applied to a hot mill in the U.S.A. since the early 1930's. This bears out the point that for a reversing hot mill the use of the double-armature twin-motor arrangement is not easy to justify with present ratings.

THE MOTOR GENERATOR SET

The reversing hot-mill motor is generally fed from a flywheel motor generator set driven by a slip-ring induction motor equipped with automatic slip-regulating control to equalize the load peaks on the supply mains. Figure 10 shows a typical set for a 4850-h.p. drive; it has a 3000-h.p. induction motor and 150,000-h.p. sec. flywheel. In some instances, of course, there is a strong case for a synchronous motor-driven motor generator set, particularly where a reversing hot mill is used for the hot-finishing of strip and where very long passes are made so that the average load on an induction motor is high in relation to its continuous rating. The mill motor in Fig. 4 is fed by such a set, equipped with automatic control of the synchronous motor excitation arranged to maintain either a constant power factor or a constant reactive kVA., irrespective of the load on the motor. Such modern automatic excitation controls are very quick-acting and can boost the excitation rapidly at load peaks. They make possible the use of a smaller motor than would be required for a constant value of excitation sufficient to meet the highest peak. In addition, there is an appreciable reduction in excitation losses over a given period.

Generators

For the reversing-mill motor an approximate relationship was given between the peak and the possible continuous rating of the machine. A generator, which is running at a much higher speed, is more difficult to design for such a high electrical loading under peak conditions. For a generator designed to have the maximum permissible electrical loading under peak conditions, the continuous rating of which it is capable will be about half of this peak as opposed to about 40% in the case of the motor.

The generator has to supply the mill motor losses and must be able to carry the peak current required by the motor. Generally, more than one generator is used; the number depends on the speed adopted for the motor generator set.

Where more than one generator in parallel is used it is usual to provide cross compounding to ensure adequate load sharing. Generators may be connected either in series or in parallel; the choice of the arrangement used is dependent upon the drive under consideration and the preference of both manufacturer and user. With multiple generators it is possible to run at a reduced rating in the event of one failing.

The Flywheel

The flywheels on modern equipment are usually of steel plate although cast steel wheels are also used. The size of the flywheel is closely tied up with the duty-cycle of the mill and each case must therefore be considered separately. In calculating the size of a flywheel it is usual to consider the maximum permissible speed drop as 15%, which means that about 28% of the total stored energy of the wheel is available for load equalization. The ratio between flywheel capacity and induction-motor horsepower may vary considerably. In certain cases where heavy peak loads are required for a fairly low output, as in the rolling of special alloys, a relatively large flywheel and small induction motor are used because the average load on the mill is low, whereas for a high-capacity blooming or slabbing mill the average load will be appreciably higher and the induction-motor rating will have to be correspondingly greater relative to the horsepower of the mill motor.

It is not unreasonable to assume that the sum of induction-motor horsepower and a suitable percentage of the available flywheel horsepower seconds will lie within fairly close limits when considered in relation to

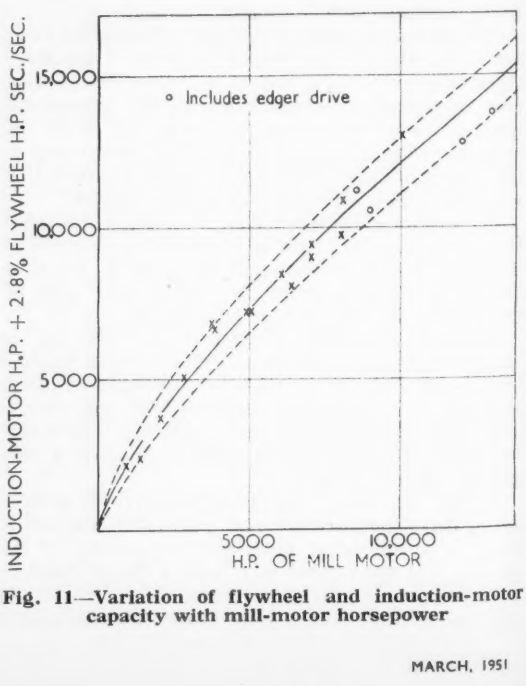


Fig. 11—Variation of flywheel and induction-motor capacity with mill-motor horsepower

the horsepower of the mill motor. Figure 11 shows the induction-motor horsepower plus the horsepower developed by the flywheel in giving up 10% of its available stored energy in one second (which is numerically equal to 2·8% of the total flywheel h.p. sec., assuming 15% permissible reduction) plotted against the horsepower of the mill motor. The points plotted from an investigation of various installations lie between quite close limits. The mean curve follows the equation

$$P + Q = 2300R^{0.722}$$

where P = induction-motor h.p.
Q = 2·8% of the total flywheel h.p. sec.
R = mill-motor h.p.

This empirical formula gives a guide to flywheel size during the initial stages of a project and gives a closer estimate than any factor which directly multiplies either the mill-motor or induction-motor horsepower to obtain a numerical value for the flywheel h.p. sec. Such factors vary from 25 to 50 for the induction motor and from 17 to 40 for the mill motor.

Band brakes are sometimes applied to the flywheels of motor generator sets to enable the set to be brought to rest quickly in an emergency, but the modern arrangement is to provide for the dynamic braking of the set by applying a direct current to the stator of the induction motor with all the rotor resistance in circuit and then to reduce the rotor resistance as the speed of the set is decreased. By this means the set can be brought to rest in about the same time as it takes to start it up ; the heat is dissipated in the cooling water of the liquid slip regulator instead of in the periphery of the flywheel brake, which often gives rise to a lot of smoke and smell. For example, one flywheel set with a 150,000-h.p. sec. flywheel which, when left to coast to rest will run for about an hour and a quarter, can easily be brought to rest in just under 2 min. by dynamic braking ; with a band brake it would probably take about 20 min. Where a band brake is not provided, it is usual to supply a screw-operated lifting band or some other form of jacking device or cradle to take the weight of the flywheel which is raised slightly so that the bearings can be removed for inspection.

The Driving Motor

The choice of D.C. generator rating for a given mill motor is simple compared with that of the induction motor driving the flywheel set. The governing factor is no longer the peak nor the r.m.s. load demanded by the mill but is the average load of the mill motor for the heaviest sustained duty which it has to meet, and this varies with the type of mill. It must also supply the mill-motor and generator losses, the windage and friction losses of the flywheel, and allowance must be made for the losses in the slip regulator, which of course pass through the induction motor. Experience shows that the r.m.s. loading of the induction motor rarely, if ever, exceeds 1·1 times its average load ; a figure of this order must be included in any factor for obtaining the r.m.s. rating of the induction motor from the average loading of the mill motor. Depending upon the efficiencies and losses already mentioned, a factor of 1·4–1·5 is commonly used for obtaining the r.m.s.

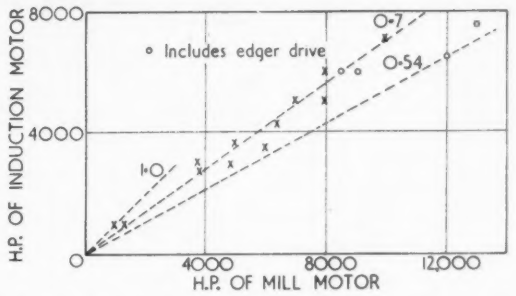


Fig. 12—Ratio of mill-motor and induction-motor ratings

horsepower of the induction motor from the average horsepower demanded from the mill motor. This results in a ratio of induction-motor rating to mill-motor rating, which varies from about 0·55 up to unity ; 0·7 is a fair average. Figure 12 shows the amount of ' spread ' which is obtained in practice.

LUBRICATION

The bearings of motor generator sets are usually self-lubricated by oil rings which may be supplemented by a flood lubrication system with pumps for circulating the oil. High-pressure ' jacking ' oil pumps are often found on flywheel bearings and, in synchronously driven sets, on the synchronous motor bearings or in some cases on all the machine bearings to assist in starting from rest. Such pumps are shut down when the set is up to speed. For higher peripheral speeds of journal, *e.g.*, above 1500 ft./min., it becomes necessary to provide additional cooling for the bearings, and this is done either by water-cooling in the bearing or by a circulating system for the lubricating oil, which enables it to be both cooled and, if desired, filtered. Some manufacturers prefer bearings to be fed under pressure at all times and a manufacturer may depart from his normal practice to suit a particular customer's preference.

In the mill motor different arrangements are again found. These vary from constant-pressure feed of oil to self-lubricated bearings, although in this case the need for special cooling hardly arises because the very low peripheral speeds of the journals provide no great difficulty for a self-cooled bearing.

The efficiency of the bearings on modern electrical machines is of interest in connection with mill-motor lubrication. During commissioning tests on a drive of nearly 5000-h.p. capacity it was found that before the motor was coupled to the mill it could be kept rotating once the lubricating film had been established with an input to the armature of between 400 and 600 W. In another case a 1000-h.p. motor continued to creep with only 80-W. input to the armature. Such creep speeds may be as low as $\frac{1}{10}$ of an r.p.m.

VENTILATION

For the 7000-h.p. drive which has been considered the total electrical losses (based on full-load efficiencies) amount to approx. 1000 kW. This loss appears in the form of heat in the machines themselves and must

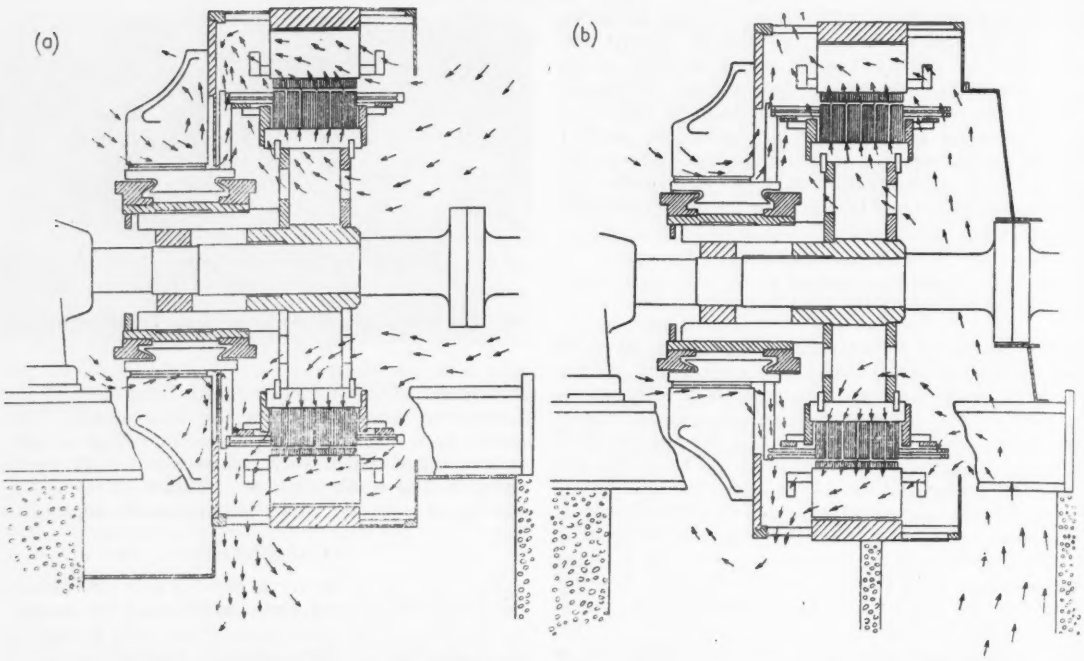


Fig. 13—Generator arrangement (*a*) for down-draught ventilation, (*b*) for up-draught ventilation

be removed by the provision of adequate ventilating air. It is not sufficient to allow the machines to ventilate themselves by using the air in the motor room as this would soon be raised to a temperature too high for comfort, unless very large ventilators were provided and this would result in the ingress of much dirt to the detriment of the electrical machines. In this country the generally accepted figures for the quantity of ventilating air used are 100–120 cu. ft./min./kW. loss. The higher figure prevails where several machines share a common supply, and the above installation would, therefore, require a total of 120,000 cu. ft. of air/min.

The most usual arrangement, the ' straight through ' system, is to have a main ventilating plant supplying the whole of the air, which is drawn by a suitable fan or fans through filtering equipment. The air is supplied at a relatively low pressure to the basement of the motor room or to the pits below the main generator set in the event of there not being a basement. The openings to the machine foundations are provided with some simple means of ensuring reasonable sharing of the air between the machines which then look after their own ventilation.

With the mill motor, however, because of its low speed, forced ventilation is required and a booster fan is used to supply this machine, drawing filtered air from the basement and blowing it into the drive end of the machine from whence it passes through the frame and armature and out into the motor room. A pressure of about $\frac{1}{2}$ in. W.G. is used in the basement and this is boosted to about 2 in. W.G. to force the air through the mill motor. It is desirable to maintain a slight pressure in the motor room above that of the external atmosphere to prevent dirt getting in. In certain cases the mill motor is not in the motor generator house and it is then usual to increase the size of the cable tunnel to the machine to carry the ventilating air, and the capacity of the booster fan is increased to deal with the extra pressure drop along the tunnel.

As the size of an installation increases, the provision of a very large amount of clean air becomes quite a major item and, as no filtering equipment is 100% perfect, over a period, plenty of dust will enter. Recourse is then made to some form of recirculating system with coolers interposed in the circuit. Such systems may include the entire motor generator house and basement ; this is a necessity with open-type machines. In certain cases the system is confined to the motor generator set or the mill motor itself ; in this case the commutators may or may not be included in the circuit and it is necessary to filter the make-up supply, involving about 10% of the total.

In regard to the machines themselves, Figs. 13*a* and *b* show the passage of the air through the generators for down-draught and up-draught ventilation respectively. In Fig. 14 the complete circuit is shown diagrammatically for a system which includes the entire motor generator house and basement.

The up-draught system, although simple to apply, is not satisfactory for a recirculating system, because the hot air is discharged into the motor room. A ventilating air quantity of 100 cu. ft./min./kW. loss through a machine gives a temperature rise of about 35° F. and the atmosphere will therefore be extremely uncomfortable in hot weather when cooling-water temperatures in this country may rise to 70 or 75° F.

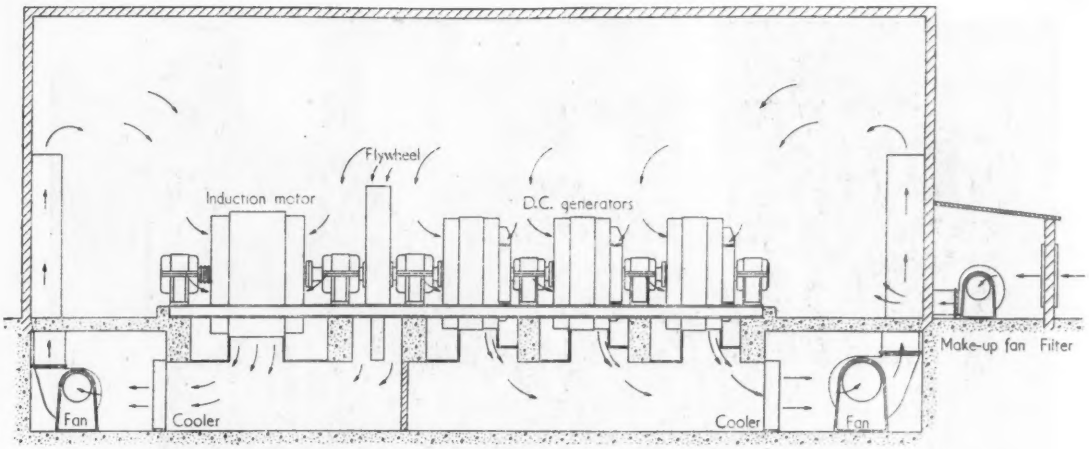


Fig. 14—Typical arrangement of M.G. house with down-draught ventilation

The same criticism can be made of the straight-through up-draught ventilating scheme when outside temperatures may be 80° F. or higher, although in the latter case relief can be obtained by using a larger quantity of air per kilowatt loss, but this only adds to the problem of filtration.

The down-draught system results in a cooler motor room, but in winter it is desirable to by-pass some of the circulating air from the coolers to keep the room temperature from falling too low, or, preferably, to reduce the water flow in the coolers. In this case, of course, the plenum must be maintained in the motor room itself, which is more complicated than maintaining it in the basement. The necessary pressure is provided by a filtered make-up supply sufficient to cope with the inevitable leaks in the circuit. The down-draught system, as indicated in Fig. 14, has the disadvantage that brush dust from the D.C. machines is drawn into the basement and, in time, tends to choke the coolers. Arrangement must, therefore, be made to clean these at regular intervals or alternatively to install filters in the air circuit ahead of the coolers.

In another arrangement the commutators are

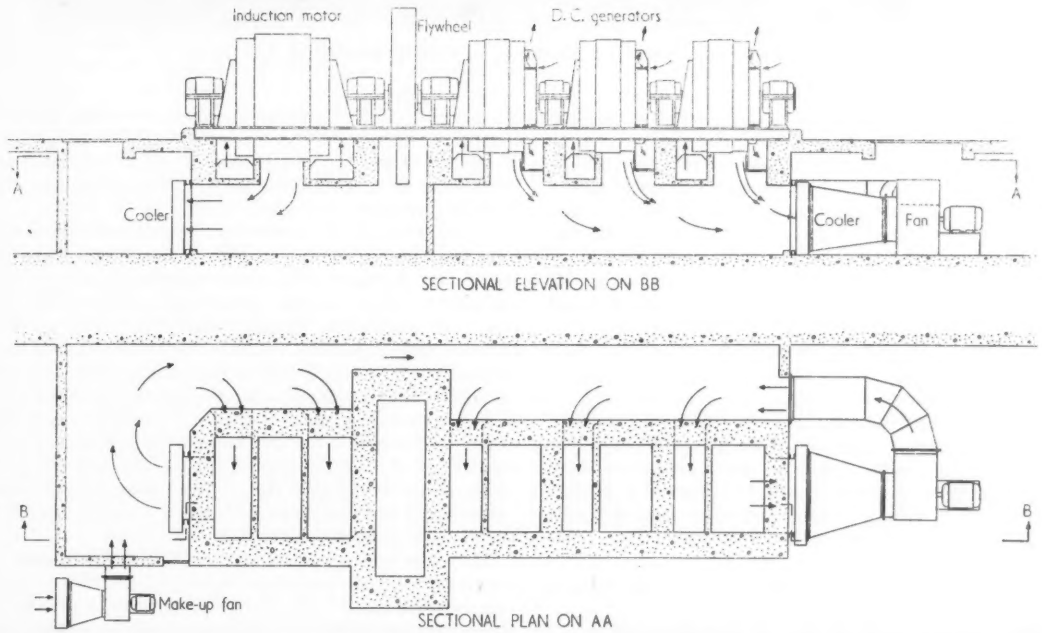


Fig. 15—Enclosed recirculating system of ventilation for a motor generator set; commutators are excluded

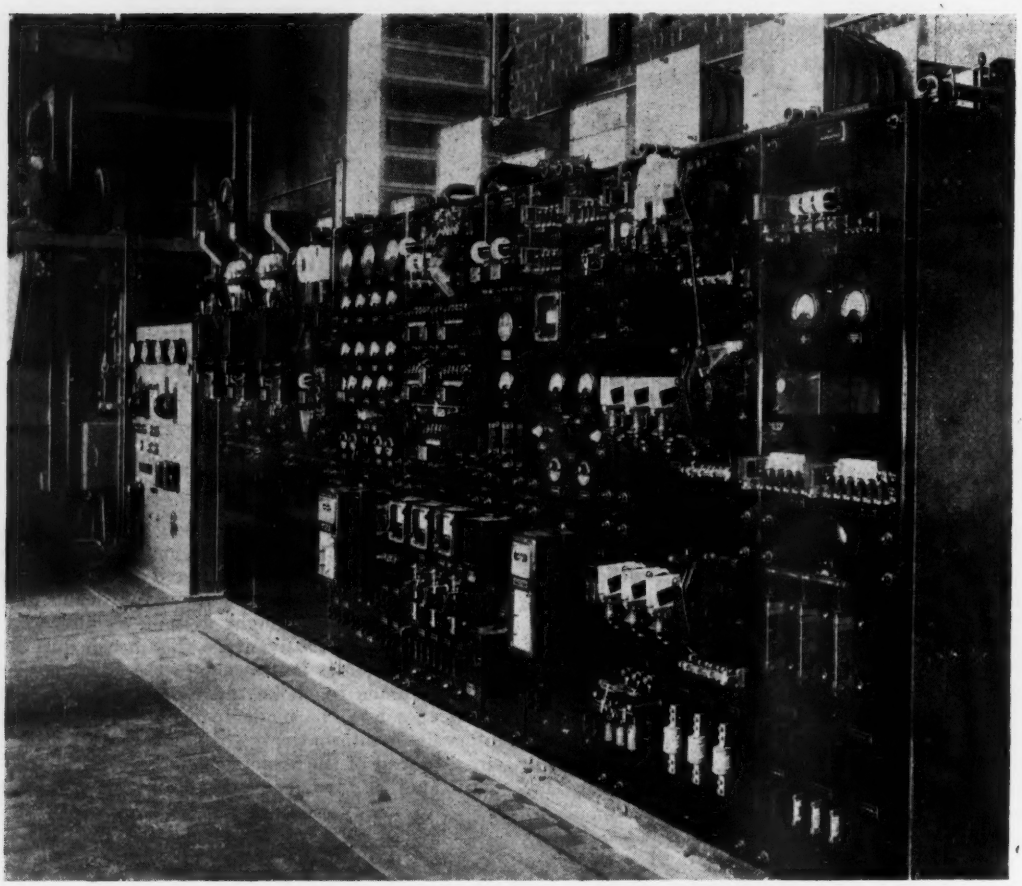


Fig. 16—Control equipment for 4850-h.p. reversing structural-mill drive

excluded from the air circuit and allowed to ventilate themselves naturally in the air in the motor house. The recirculating system maintains a pressure within the machines so that any leakage is out into the motor room over the commutators, thus preventing the ingress of brush dust to the circuit and avoiding the danger of choking the coolers. An arrangement of this kind for a motor generator set is shown in Fig. 15, where the induction motor circulates its own air and the generators have a common circulating fan.

A further possible development is to enclose the machines and to use filters in the air circuit to collect the brush dust.

A good ventilating scheme requires much thought ; from the electrical manufacturers' point of view the author would make a plea for more adequate consideration to be given in the early stages of a project to avoid the cramped and awkward arrangements which have to be adopted in many cases.

CONTROL GEAR

Requirements and Capabilities of Modern Control Schemes

The modern rotating type of regulator, of which the amplidyne is a well-known example, has resulted in marked simplification and improvement in the control gear for reversing hot-mill drives. A minimum of contactor gear is required and all necessary contactors are of small dimensions handling light currents and they require very little maintenance.

Figure 16 includes the entire control gear required for a modern reversing hot mill, with the exception of the circuit breaker feeding the induction motor and the control desk in the mill pulpit. From left to right are shown the liquid starter-slip regulator for the slip-ring induction motor of the flywheel motor generator set, a small cubicle from which the main oil circuit breaker is controlled and which contains change-over switches for normal operation or dynamic braking of the motor generator set, and a switch for the hand control of the liquid slip regulator for the purpose of dynamic braking. The control gear also carries metering, recording, and indicating instruments for the induction-motor circuits.

The main control panel carries D.C. circuit breakers for the three generators feeding the mill motor ; these can be dispensed with if the generators and motor armatures are in series. The four centre sections of panel carry the instruments required for the super-

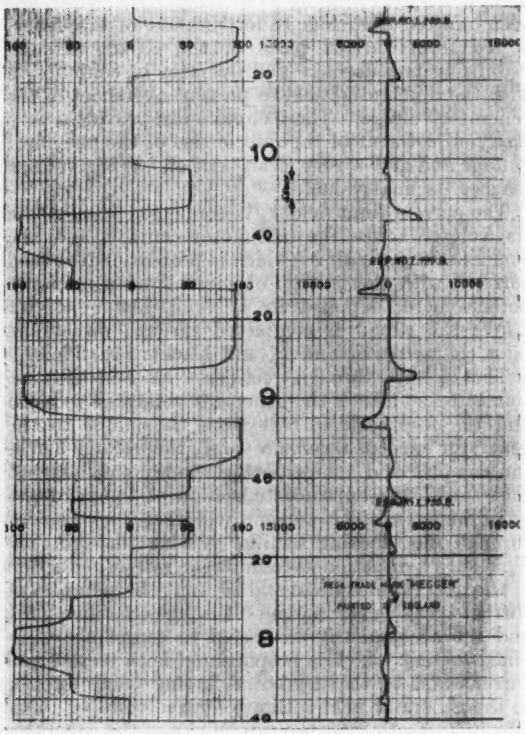


Fig. 17—Recorder chart of response of 4000-h.p., 50–100-r.p.m. reversing plate mill drive

vision, control, and alarm annunciation of the main mill drive, and the remaining two sections of panel on the right (Fig. 16) are solely for starting the main and pilot exciter sets and for jacking oil pumps for the flywheel of the motor generator set. The only other control gear directly associated with the mill drive is that for the ventilating equipment, which is housed elsewhere as it is common to more than one drive.

A recent modernization of an existing plant where the amplidyne control involved only a 4-ft. wide panel is an even better example.

Smaller space is not, however, the only advantage of the modern equipment. The equipment also enables the utmost use to be made of the available torque, speed, and acceleration of the drive and greatly reduces the possibility of tripping the main circuit breakers or overload relays by a driving error because a driver may operate his lever very rapidly without danger.

In the early days of reversing-mill control, the direct control of the fields of the generators and motors by contactor gear was tried but it soon became necessary to interpose exciters and to control the fields of the exciters. This in itself was a big step forward for it enabled a very simple method of forcing to be applied to the large machine fields by the use of differentially wound exciters. The major problem which the control scheme for a reversing-mill drive has to overcome is the build-up and decay of the main generator and particularly of the mill-motor fields. These fields have a very high inductance and, because of the large masses of iron in the magnetic circuits, eddy currents result which cause the fluxes in the field to lag appreciably behind the exciting currents. This effect can be mitigated by the use of laminated poles in the main machines. Occasionally the main generators have even been built with laminated frames, but this is a very expensive construction and could certainly not be applied to the mill motor itself. The degree of forcing applied by the exciters has been increased, but this results in overshoot of the generator voltage unless the degree of forcing is appreciably reduced before the final values are obtained.

With a modern control scheme it is possible to bring the control still a stage further back to the rotating regulator fields, and only very light control currents are necessary. These rotating regulators respond very rapidly, and with correctly designed control circuits it is possible to apply high rates of forcing, over 90–95% of the accelerating range, and still provide an effective check to prevent overshoot.

With a means of control which will give the required rates of acceleration and retardation, steps must be taken to ensure that this cannot be abused. If the driver chooses to swing the controller straight over from full speed forward to full speed reverse, and if the generator field were to reverse at its maximum rate of change, then dangerously high feed-back currents would be generated with inevitable tripping on overload. The regulating machine can, however, have a number of field windings and it is possible to use one of these in a suitable reference circuit to provide a current-limiting control which will retard the rate of decay of the generator voltage to hold the pump-back current to a permissible maximum. The rate of growth of the generator voltage during acceleration may also be regulated to provide extra torque for rolling should an excessively heavy pass be taken. Although the gain here cannot be very great, nor must it entirely prevent acceleration, it can often prevent a trip on overload when for some reason the torque is above maximum.

Similarly, a very effective control can be applied to the mill-motor field. This will prevent field weakening beyond a point indicated by the permissible load current in the motor, and with a suitable blending of these controls it is virtually impossible for the driver to overload the machines by mishandling his controller, and overload tripping will take place only if the actual torque demanded from the motor by the metal in the rolls exceeds the specified limit.

Figure 17 shows a recorder chart taken on a 4000-h.p. universal reversing plate mill drive to which an amplidyne control was added after the mill had been running for many years. The speed curves, which were taken without metal in the mill, show a clean shape, and the relatively flat tops of the current curves for reversals from full speed forward to full speed reverse and *vice versa*, and from full speed forward to base speed reverse and *vice versa*, show the effectiveness of the current-limit control operating through the amplidyne.

The accelerating curve under motor field weakening is relatively straight, and although the slope was

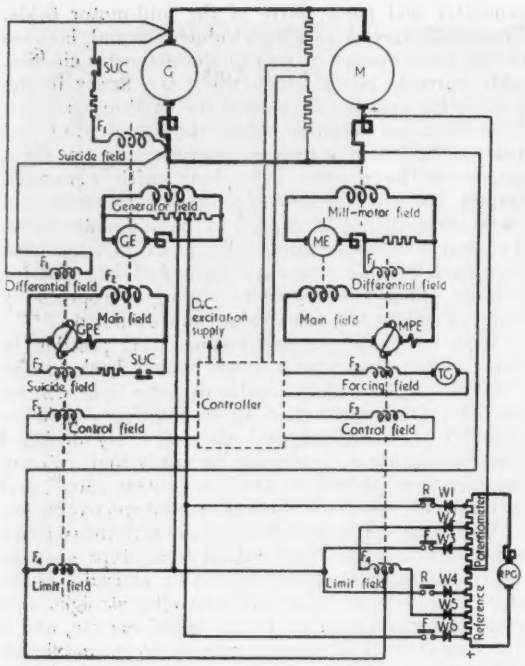


Fig. 18.—Simplified key diagram of control scheme for reversing hot-mill drive

actually limited by the capacity of the existing exciters it gives, under this condition, an accelerating time of 5 seconds.

Control Circuits

A typical control system in a simplified form is shown in Fig. 18, and both generator and motor are excited by separate main exciters, *GE* and *ME* respectively, which in turn are excited by the amplidynes *GPE* and *MPE* respectively.

Generator Control—In the generator control the pilot exciter *GPE* has several separate field windings and one of these, F_3, is the control field supplied from the mill operator's controller. The excitation applied to this field may be 10–15 times the net value required to maintain the desired generator volts, with the result that the voltage of this machine immediately rises to its ceiling value. This compels the field, and consequently the voltage of the main exciter *GE*, to rise very rapidly. This machine is designed to produce a voltage several times higher than that required for normal excitation and this in turn causes the generator-field current to be forced up very rapidly. The field F_1 on *GPE* is fed from across the generator armature and is therefore excited proportionally to the generator volts. This field, which opposes the control field, will eventually restore the net excitation of the pilot exciter to the value required to maintain a steady generator voltage. As the control field takes the pilot exciter well beyond its saturation point the generator voltage will rise to quite a high percentage of its final value before the differential field starts to reduce the degree of forcing.

A further field winding on the amplidyne, which has been omitted from Fig. 18 for the sake of simplicity, is fed from various parts of the circuit through stabilizing transformers and operates in conjunction with the differential field to prevent overshoot of the generator voltage. These circuits can provide a very ready adjustment of the rate of rise of the generator voltage and give a very clean ' cut off ' as the voltage reaches its final value.

The current-limit field F_4 is connected to compare the voltage across the mill-motor compole and the compensating windings (which is proportional to load current) with the voltage obtained from a reference potentiometer supplied by a small pilot generator *RPG*; the circuit is completed through directional pilot contactors *R* and *F*, and blocking rectifiers.

The circuits through rectifiers *W*3 and *W*4 are connected so that if the generator voltage is reduced and the feed-back current from the motor to the generator exceeds a certain value, the voltage across the compole and compensating windings will exceed that across the appropriate section of the reference potentiometer. The current will then ' spill over ' through the blocking rectifier passing through field F_4 in such a direction as to limit the rate of reduction of the generator voltage and so limit the pump-back current peak. The circuits through rectifiers *W*1 and *W*6 are similarly connected to compare a combined voltage which is proportional both to the load current and to the degree of forcing being applied to the generator field with a reference voltage. This circuit serves to limit the rate of rise of the generator volts and therefore of the acceleration of the mill motor in the event of an excessive rolling load.

Motor-Field Control—In the motor-field control, which operates through the pilot exciter *MPE*, the control field F_3 has again applied a degree of excitation many times that required for the maintenance of the final value. This is balanced by the differential field F_1 fed proportionally to the mill-motor field current from across a resistance in series with the mill-motor field. Variations in the control field result in a forcing effect very similar to that already described for the generator but, as already explained, the actual flux of the mill motor may lag appreciably behind the current exciting it and consequently the forcing of the motor field will cease before the flux has reached the final

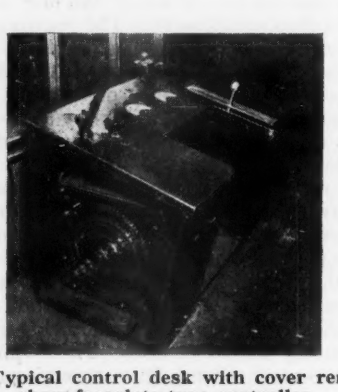


Fig. 19—Typical control desk with cover removed to show faceplate-type controller

required value. To allow for this the field F_2 on *MPE* is used to compare the mill-motor speed (from the tacho-generator) with the actual speed demanded by the setting of the controller, and it serves to maintain the forcing on the mill-motor field until the mill-motor speed itself has reached the required value. This circuit is compensated to allow for variations in voltage across the mill motor which might otherwise give a false impression of mismatching between actual speed and controller position.

In the motor-field control a current-limit field F_4 is again used to compare the voltage across the compole and compensating windings with the reference potentiometer in such a way as to prevent weakening of the mill-motor field if the current in the mill-motor armature exceeds a predetermined value.

By connecting the controller in the amplidyne field circuits only, very small control currents are required and a very light controller is possible. A simple faceplate rheostat-type of controller, which is operated through a special lever gear, has been adopted. This is found to give very easy and smooth operation and is well liked by operators. The controller is mounted in a control desk which is shown in Fig. 19. Desks such as this have been produced in various forms, some with a screwdown controller in the other wing of the desk, some with the kneehole extended through the desk to give better visibility when the pulpit is rather close to the mill, and others with two adjacent levers in the left wing of the desk which are used for front- and back-feed tables.

Pulpit arrangements vary appreciably between different installations and vary in the way in which the work is divided between a team of operators. Co-ordination in determining the arrangement is needed at an early stage and at a level which includes someone who will be closely connected with the operation of the mill. The electrical manufacturer may group his controls in a neat and satisfactory way in a desk but, shortly before or after installation, various additional devices are added and the final arrangement is built up piecemeal. Particular care should be taken where hydraulic controls are adjacent to electrical controls, both from the electric and aesthetic aspect.

On American installations a foot-operated master controller for the mill drive is quite general and the mill operator also has control of the front and back tables and screws with approach-table controls probably within reach. The operator in the pulpit is responsible for manipulators, for tilting and turning, and for the runout table.

In this country it is more common to have one man responsible for the mill drive with two other members of the team to deal with all the various auxiliary movements. Where there is more than one stand of rolls the division of controls has to be still further broken down and no hard and fast rules can be given.

PERFORMANCE

With a modern control scheme a reversing-mill drive can be reversed from base speed to base speed within 1½ sec. without much difficulty and, with auxiliary controls matched to the mill drive, such reversal times are definitely worth while, since it has been shown[3] that it is possible to return a slab or ingot into the rolls in a little over 1½ sec.

It might well be queried whether such fast reversing times are justifiable in view of other inevitable delays which mean that the mill can never be worked at the full rate continuously. There is an advantage, however, in being able to roll a single slab quickly, because the average temperature of the steel during the rolling process is higher and consequently less energy is absorbed in the process.

The higher the base speed of the mill the longer will be the accelerating time required to reach that speed unless an undue proportion of torque is taken up in bringing about the acceleration. For example, the single-armature twin-motor drive already considered could be accelerated to base speed (50 r.p.m.) in 1¼ sec. with just under 40% of full-load current, which leaves, for metal entering the rolls during acceleration, a little more than twice full-load torque available for rolling. If possible the torque required to accelerate the motor to base speed should not be allowed to exceed half full-load torque, which means that where larger inertias are involved the rate of acceleration should be reduced.

Acknowledgments

The author wishes to thank the British Thomson-Houston Co., Ltd., for permission to publish this paper and to acknowledge the assistance of various colleagues in the preparation of diagrams and other data which have been used.

References

1. W. L. STOVER : *Iron and Steel Engineer*, 1948, vol. 25, p. 62, July.
2. R. H. WRIGHT : *Ibid.*, p. 125, Sept.
3. W. E. MILLER : *Blast Furnace and Steel Plant*, 1950, vol. 38 p. 248.

SPANISH INSTITUTE OF WELDING

The third competition for papers on subjects relating to welding has been arranged by the Instituto de la Soldadura (Spanish Institute of Welding). The following prizes are offered for papers submitted not later than 1st October, 1951 :

(i) The special " José Rubí " prize of 8000 pesetas for a paper on the study of the deformations which result from autogenous welding (oxyacetylene and electric-arc) and gas cutting, their consequence and counter measures.

(ii) A prize of 4000 pesetas for the best paper presented, and three prizes of 2000 pesetas each, for papers on different subjects selected from :

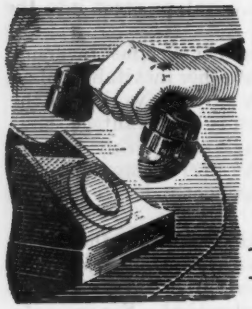
(*a*) The application of micro-analysis to welding problems

(*b*) The determination and study of the residual stresses in welded construction

(*c*) The economy in metal resulting from the substitution of welding for other methods of construction in the manufacture of machines and metallic structures

(*d*) The entrant's own choice.

The conditions of award and further particulars may be obtained from the Secretariat of the Instituto, Goya 58, Madrid.



N E W S

ANNOUNCEMENTS AND NEWS OF SCIENCE AND INDUSTRY

THE IRON AND STEEL INSTITUTE

Meetings

Annual General Meeting

The Annual General Meeting of the Institute will be held in London from 30th May to 1st June, 1951.

Special Meeting in Austria

The Council has accepted an invitation from the Austrian Iron and Steel Institute, to hold a Special Meeting in Austria from 5th to 19th September, 1951.

Autumn General Meeting, 1951

The Autumn General Meeting of the Institute will be held in London on **Wednesday and Thursday, 21st and 22nd November, 1951,** and not on the 14th and 15th November as announced in the September, 1950, issue of the *Journal*.

NEWS OF MEMBERS

▶ Mr. F. W. J. BAILEY has left The Mond Nickel Co., Ltd., to take up an appointment as Technical Assistant to the Manager, Hot Rolling Department, Messrs. Henry Wiggin and Co., Ltd., Birmingham.

▶ Mr. S. N. CHATTERJEE has left Messrs. Stewarts and Lloyds, Ltd., to join the Steel Corporation of Bengal, Ltd., Burnpur, India.

▶ Mr. S. G. COPE has returned from five years' service with the Control Commission in Germany and is now a Senior Lecturer in Metallurgy at the Sir John Cass College, London.

▶ Mr. J. CROWTHER, formerly research metallurgist with Messrs. James Booth and Co., Ltd., Birmingham, has been appointed Chief Metallurgist with the same company.

▶ Mr. J. D. ELLIS, formerly London Manager of the Birmingham Battery and Metal Co., Ltd., retired in June, 1950, after over 20 years' service with the Company.

▶ Dr. P. K. GLEDHILL has left Messrs. Stewarts and Lloyds, Ltd., to take up an appointment with the British Iron and Steel Research Association, North East Coast Laboratory, Normanby, Middlesbrough.

▶ Mr. F. J. HEWITT has transferred to Fellowship of the Institution of Metallurgists. He has left the Park Gate Iron and Steel Co., Ltd., and is now in charge of Coke Research at the Rotherwood Research Laboratories of the United Coke and Chemical Co., Ltd.

▶ Mr. H. HUMPHRIES has been appointed Deputy Managing Director of Hadfields Ltd., East Hecla Works, Sheffield, in succession to Mr. J. B. Thomas.

▶ Mr. A. G. KNAPTON, of the A.E.I. Research Laboratories, has been awarded the Ph.D. degree of the University of Wales.

▶ Mr. C. LANG has left Messrs. Stewarts and Lloyds, Ltd., and is now at the British Iron and Steel Research Association, North East Coast Laboratory, Normanby, Middlesbrough.

▶ Mr. S. K. MITRA has left the Bhartia Electric Steel Co., Ltd., Ballygunde, and is now metallurgist to the Directorate of Industries in Calcutta.

▶ J. H. PALM has left the National Luchtvaart Laboratorium, Amsterdam, and is now Head of the Laboratory of Valevefa NV, Amsterdam.

▶ Mr. C. D. SHOWELL has left the Northern Aluminium Co., Rogerstone, and is now Managing Director of Edwin Showell and Sons, Ltd., Birmingham.

▶ Mr. E. SUNSTRÖM, of Gunnebo Bruks Aktiebolag, has been appointed manager of the company's subsidiary in Varberg, Sweden.

▶ Mr. B. N. H. THORNELY has left the Northern Aluminium Co., Ltd., to become Director of Operations with Aluminium Ltd., Montreal, Canada.

▶ Mr. T. H. TURNER, formerly chief chemist and metallurgist, Railway Executive, Doncaster, has been appointed Superintendent (Met. Div.) of the Railway Executive Research Department, Derby.

Obituary

Mr. B. A. ALBORG, of Finland, in the summer of 1949. It is regretted that this information has only now been received.

Mr. R. L. BRUNTON, Resident Welding Service Engineer of the Acetone Illuminating and Welding Co., Ltd., Hamilton, New Zealand, on 7th April, 1950.

Mr. K. W. WINSOR, Director and General Production Manager, E.M.F. Electric Company, Victoria, New South Wales, on 18th June, 1950.

Professor T. TURNER, an Honorary Vice-President of the Institute and a Bessemer Gold Medallist, on 31st January, 1951.

CONTRIBUTORS TO THE JOURNAL

N. D. Macdonald—On the staff of the Ironworks of the Appleby-Frodingham Steel Company. On leaving the Scunthorpe Technical School, Mr. Macdonald entered the laboratory of the Appleby-Frodingham Steel Company, and in 1945 transferred to the Ironworks of the same Company.

E. D. Harry, B.Sc.—Research Metallurgist with The Steel Company of Wales (Lysaght Works). Mr. Harry graduated with honours in metallurgy from University

V. Kondic

D. R. G. Davies

W. O. Binder

College, Swansea, in 1942. He spent three years as assistant metallurgist in the Metallurgical Research Department of the Appleby-Frodingham Steel Company, followed by a period of employment as assistant metallurgist with the Workington Iron and Steel Company. Mr. Harry is a part-time lecturer in metallurgy at the Newport Technical College.

Stanley Rushton, M.A., B.Sc., D.I.C.—Lecturer in the Department of Mathematics, Imperial College, London University. Mr. Rushton is a former Scholar of Queen's College, Oxford University. He carried out post-graduate work in mathematical statistics at Imperial College before being appointed to his present position of Lecturer. Mr. Rushton is a Fellow of the Royal Statistical Society.

D. R. G. Davies, B.Sc., A.I.M.—Chief Metallurgist at the Redbourn Works of Messrs. Richard Thomas and Baldwins, Ltd., Scunthorpe. Mr. Davies was born at Gowerton, Glam., in 1921 and was educated at Builth Wells County Grammar School and at University College, Swansea, where he received an honours degree in metallurgy, He was employed at Exors. of James Mills, Ltd., Woodley, and subsequently in the Research Department of Messrs. Hadfields Ltd., Sheffield. He was later appointed Chief Metallurgist in the Panteg Works of Messrs. Richard Thomas and Baldwins, Ltd., Griffithstown, Mon.

In 1948 Mr. Davies was awarded a Mond Nickel Fellowship which enabled him to study in the Mathematics Department, Imperial College, London, and later to tour the Universities and steelplants in the U.S.A. and Canada. On his return he took up his present appointment in Scunthorpe.

V. Kondic, B.Sc., Ph.D.—Lecturer in the Industrial Metallurgy Department of Birmingham University. Dr. Kondic graduated in Mechanical Engineering at Belgrade University in 1937, and following a period of military service was employed as designer at an engineering firm. He won a post-graduate scholarship in England in 1938 and obtained his Ph.D. degree in metallurgy at Birmingham University in 1942. He has since held appointments with the Tin Research Institute working on researches in bronze castings, and with High Duty Alloys, Ltd., working on continuous casting. Before taking up his present appointment he was employed by the British Welding Research Association on steel-welding research. Dr. Kondic is the author of many papers on metallurgical subjects, mainly on the metallurgy of melting and casting.

The following are biographies of authors of papers published in the February issue of the Journal:

William O. Binder—Research Metallurgist at the Union Carbide and Carbon Research Laboratories, Inc., Niagara Falls, N.Y. Mr. Binder graduated B.S. from the Case Institute of Technology in 1931 and received the degree of Metallurgical Engineer in 1936. From 1931 to 1936 he worked in the metallurgical department of The Dow Chemical Company, Midland, Michigan, on an investigation of magnesium and magnesium-base alloys. In 1936 he joined the Union Carbide and Carbon Research Laboratories, Inc., and in 1946 he took charge of the research work on high-chromium steels and alloys for corrosion and heat-resistant purposes. Mr. Binder has published a number of papers on the properties and corrosion resistance of stainless steels and has assisted in the development of some of the cast and wrought high-temperature alloys containing cobalt for gas-turbine application.

Mr. Binder is Chairman of the High-Alloys Committee of the Welding Research Council of the Engineering Foundation, and in 1950 was one of the recipients of the Henry Marion Howe Medal awarded by the American Society of Metals.

H. T. Protheroe, B.Sc.—Lecturer in Foundry Metallurgy at Sheffield University. Mr. Protheroe was educated at Maesycwmmer Secondary School, Mon., and in 1931 graduated B.Sc. (Metallurgy) from Cardiff University. From 1931–1935 he was chief assistant Metallurgist and steel-blower at Powell Duffryn Engineering Works, Tredomen Ystrad Mynach. He took up his present position in 1935.

Mr. Protheroe has periodically contributed to the technical press. He is a member of the Melting and Metallurgical Committee and the Physical Properties Committee of the British Iron and Steel Research Association, and is Past-Chairman of the Association's Non-destructive Testing Committee.

IRON AND STEEL ENGINEERS GROUP

The **Fourteenth Meeting** of the Group will be held at 4 Grosvenor Gardens, London, S.W.1, on **Thursday, 5th April, 1951.** The programme for the Meeting is as follows:

10.30. A.M.—Discussion on the paper "*Electric Drives for Reversing Hot Mills*," by P. E. Peck (this issue of the *Journal*).

12.45. P.M.—Buffet Luncheon in the Library

2.0–4.30 P.M.—Discussion on the paper "*Hot Bloom and Slab Shears*," by J. A. Kilby (February, 1951, issue of the *Journal*).

BRITISH IRON AND STEEL RESEARCH ASSOCIATION

Corrosion

Dr. U. R. Evans, F.R.S., of the Department of Metallurgy, Cambridge University, will give a public lecture on "*Fundamental Studies of Corrosion and their Importance to the Engineer*," on Wednesday, 28th March, at the Institution of Civil Engineers, London, at 5.30 p.m. Mr. T. M. Herbert, Director of Research, Railway Executive, will be in the Chair. Tea will be served at 4.45 p.m.

North East Coast Laboratory

The Iron Making Division of the British Iron and Steel Research Association has established a laboratory for ore preparation and blast-furnace research at Normanby, near Middlesbrough. It will be known as the British Iron and Steel Research Association North East Coast Laboratory and is primarily a headquarters for the investigations being carried out in the works of iron-making firms in that area. Messrs. Dorman Long and Co., Ltd., have kindly provided accommodation and services at their Redcar Works for pilot-scale work on sintering and other methods of ore preparation.

AFFILIATED LOCAL SOCIETIES

Ebbw Vale Metallurgical Society

The Joint Meeting of the Ebbw Vale Metallurgical Society and The Iron and Steel Institute will now be held on Wednesday, 14th March, 1951, and not on 15th March, as previously announced.

INSTITUTE OF METALS

The Council of the Institute of Metals has made the following awards of medals for 1951 :

THE INSTITUTE OF METALS (PLATINUM) MEDAL. To Dr. R. W. DIAMOND, Vice-President and General Manager of The Consolidated Mining and Smelting Company of Canada, Ltd., Trail, B.C., Canada, in recognition of his outstanding services to the non-ferrous metal industries in connection with researches on differential flotation as applied to the complex Sullivan ore, and as manager of the largest combined copper and lead producer in the world.

THE W. H. A. ROBERTSON MEDAL. To Mr. C. SMITH, Chief Metallurgist, James Booth and Company, Ltd., Birmingham, for his paper on "The Extrusion of Aluminium Alloys" (*Journal of the Institute of Metals*, 1950, vol. 76).

THE WALTER ROSENHAIN MEDAL (first award). To Professor G. V. RAYNOR, D.Sc., D.Phil., M.A., Professor of Metal Physics at the University of Birmingham, for his outstanding contributions in the field of physical metallurgy, in connection with our knowledge of the constitution and formation of alloys.

INSTITUTION OF METALLURGISTS

Examinations, 1951

The next examination for the licentiateship and associateship of the Institution of Metallurgists will be held from 27th August to 3rd September, 1951. Candidates must submit their applications for permission to enter the examination before 1st May, 1951. Each application must be made on a form to be obtained from the Registrar-Secretary, The Institution of Metallurgists, 4 Grosvenor Gardens, London, S.W.1, and must be accompanied by a registration fee of one guinea.

Examinations for the fellowship will also be held between 27th August and 3rd September, 1951, and intending candidates should apply for permission to enter the examination, submitting, for the approval of the Council, a statement of the branch of metallurgy in which they offer themselves for examination.

NEWS OF SCIENCE AND INDUSTRY

Internal Friction of Solids

A Joint Meeting of the Physical Society Acoustics Group and the Institute of Metals, in association with The Iron and Steel Institute, on the Internal Friction of Solids, will be held on 15th March, 1951, at 4 Grosvenor Gardens, London, S.W.1, at 2.30 P.M.

DIARY

6th Mar.—SHEFFIELD METALLURGICAL ASSOCIATION—(*Any Questions*)—Grand Hotel, Sheffield, 7.0 P.M.

7th Mar.—NORTH WALES METALLURGICAL SOCIETY—"*Titanium, the Metal of the Future*," by L. Hall—County Primary School, Plymouth Street, Shotton, Chester, 7.15 P.M.

8th Mar.—WEST OF SCOTLAND IRON AND STEEL INSTITUTE (Joint Meeting with Engineers' Group)—"*Hot Bloom and Slab Shears*," by J. A. Kilby—39 Elmbank Crescent, Glasgow, 6.45 P.M.

8th Mar. — LIVERPOOL METALLURGICAL SOCIETY — "*Atomic Structure and the Hardness of Metals*," by H. O'Neill—Electricity Service Centre, Whitechapel, Liverpool, 7.0 P.M.

14th Mar.—EBBW VALE METALLURGICAL SOCIETY (Joint Meeting with The Iron and Steel Institute)—"*Some Metallographic Aspects of the Deformation of Iron*," by H. O'Neill—Lecture Room, Messrs. Richard Thomas and Baldwins, Ltd., Ebbw Vale, 7.15 P.M.

15th Mar.—STAFFORDSHIRE IRON AND STEEL INSTITUTE—"*Should a Works Metallurgist begin his Career in the Laboratory*" (General Discussion)—Star and Garter Hotel, Victoria Street, Wolverhampton, 7.30 P.M.

15th Mar.—PHYSICAL SOCIETY ACOUSTICS GROUP AND THE INSTITUTE OF METALS (in association with The Iron and Steel Institute)—"*Internal Friction of Solids*"—4 Grosvenor Gardens, London, S.W.1, 2.30 P.M.

16th Mar.—SHEFFIELD METALLURGICAL ASSOCIATION (Modern Methods of Analysis Group)—"*The Analysis of Tungsten Carbides*," by B. Bagshawe—Mining Lecture Theatre, St. George's Square, Sheffield, 7.0 P.M.

16th Mar.—WEST OF SCOTLAND IRON AND STEEL INSTITUTE—"*Open-Hearth Furnace Practice*," by W. G. Cameron and A. I. Aitken—39 Elmbank Crescent, Glasgow, 6.45 P.M.

17th Mar.—SWANSEA AND DISTRICT METALLURGICAL SOCIETY—"*Theory, Experiment, and Practice in Sheet and Strip Rolling*," by N. H. Polakowski—Central Library, Swansea, 6.30 P.M.

28th Mar.—BISRA—"*Fundamental Studies of Corrosion and their Importance to the Engineer*," by U. R. Evans—Institution of Civil Engineers, Great George Street, London, S.W.1, 5.30 P.M.

TRANSLATION SERVICE

(The previous announcement was made in the February, 1951, issue of the *Journal*, p. 206).

TRANSLATIONS AVAILABLE

No. 417 (German). G. NAESER and F. ZIRM : "Rolling of Strip from Iron Powder." (*Stahl und Eisen*, 1950, vol. 70, Oct. 26, pp. 995–1003 ; discussion, pp. 1003–1004 (abridged)). (Translated by Mr. R. A. Hetzig and made available through the courtesy of Powder Metallurgy, Ltd., London).

No. 418 (German). G. NIEBCH and A. POMP : "Influence of Various Heat-Treatments and Alloy Additions on the Cold-Working of Steel." (*Stahl und Eisen*, 1950, vol. 70, Dec. 7, pp. 1166–1173 ; discussion, pp. 1173–1174).

ABSTRACTS OF CURRENT LITERATURE *and* BOOK NOTICES

IRON AND STEEL MANUFACTURE AND RELATED SUBJECTS

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MINERAL RESOURCES

Iron Ore Deposits in the Southern Part of the Wielun District. S. Kontkiewicz. (Hutnik, 1950, vol. 17, July–Aug., pp. 175–182). [In Polish]. The geology of iron ore deposits in the Wielun district, the mining which has been conducted there, and the chemical composition of the iron ore are described. The exploratory work and prospects for the future development are discussed.—V. G.

The Swedish Iron and Steel Industry. H. U. Ross. (Canadian Mining Journal, 1950, vol. 71, Jan., pp. 49–54). The Swedish iron and steel industry is surveyed. The Kiirunavaara mine is described quite fully, and several mines in Berslagen are mentioned. Because of Sweden's pure raw materials and lack of coal, the industry concentrates on producing high-grade special steels.—T. E. D.

The Groundhog Coalfield, British Columbia. A. F. Buckham and B. A. Latour. (Geological Survey of Canada, 1950, Bulletin 16). This report deals fully with the location, means of access, and history of the Groundhog area and coalfield which lies in one of the most inaccessible parts of British Columbia.—R. A. R.

Magnesite Deposits of Central Ceará, Brazil. A. J. Bodenlos. (United States Geological Survey, 1950, Bulletin 962-C). Two large deposits of high-grade magnesite have recently been discovered in Brazil; a geological report on them is presented. Production increased from 536 tons in 1942 to 2770 tons in 1945.—R. A. R.

The Electric Furnace Industry in Argentina. G. Zuliani. (Journal du Four Electrique, 1950, vol. 59, Mar.–Apr., pp. 41–42). In this brief survey, the mineral resources and the proposed development of the electric power supplies in Argentina are described. An existing plant of single-phase electric furnaces for the production of calcium carbide is mentioned. Large three-phase furnaces, designed to be used for the production of calcium carbide and iron alloys, are also described.—J. A. L.

ORES—MINING AND TREATMENT

Hydromechanical Beneficiation at Calbecht. A. Goltz. (Stahl und Eisen, 1950, vol. 70, June 8, pp. 505–506). The methods employed for concentrating deep-mined ore from the Salzgitter region are described. The procedure uses a succession of crushing, sieving, and washing stages in which the rock and earth are separated as coarse particles and the ore recovered as a sludge. Methods for working up the sludge and dehydrating the concentrate are indicated.—J. P.

Sintering of Magnetite Fines. G. Gianasso. (Metallurgia Italiana, 1950, vol. 42, July, pp. 268–272). The results of a series of laboratory experiments are given, which show that with the sintering of iron ores it is possible to get an agglutinated and consistent substance, porous and reactive when the sintering is carried out at a temperature between 900° and 1110° C. in an oxidizing atmosphere. This permits the sintering procedure to be modified; the reduction in temperature leading to a corresponding reduction in cost while retaining the quality of the product. Results obtained with an experimental plant operating under these conditions and using very fine mineral dust, considerably enriched, show that it is possible to obtain better products by these methods than by any other known means.—H. D. J. B.

Method for Determining Solubility Characteristics of Minerals and Rocks in Water. S. Mörstell and L. Bruneau. (Jernkontorets Annaler, 1950, vol. 134, No. 5, pp. 222–228. [In Swedish]. The electrolytic conductive power of distilled water, brought in contact with several different mineral powders, was investigated, either by dispersing the mineral powder in water and maintaining the dispersion by stirring, or by filtering the water through a layer of the mineral powder. The quantity dissoved by the water was found to be greater when the latter method was applied. By repeated leaching or filtering for a long time the leaching effect diminishes, which means that the solubility characteristics of the surfaces of the different minerals during the leaching alter with time. Small impurities in the minerals are important in this respect. B. S. E.

Oxidation of Magnetite Concentrates. J. D. Zetterstrom. (United States Bureau of Mines, Sept., 1950, Report of Investigations No. 4728). The oxidation of magnetite pellets derived from concentrates and natural sources has been investigated over the temperature range 400–1000° C. The oxidation occurred in two stages. the first being complete in 4–16 min. The extent of oxidation at 400° C. during this stage was very low, but at 1000° C. was practically complete for all but one sample. A change in the oxygen content of the oxidizing gas had a greater effect on the rate of oxidation than an equivalent change in the gas velocity. Pellets having the lowest specific gravity were, in general, more readily oxidized, the one exception to this had a larger particle size. Oxidation was accompanied by shrinkage; therefore, the greater reducibility of the oxidized pellets cannot be explained by an increase in porosity.—B. G. B.

Water-Sealed Wind Boxes for Dwight and Lloyd Sintering Machine. E. M. Tittmann and E. A. Hase. (Transactions of the American Institute of Mining and Metallurgical Engineers, 1950, vol. 188, pp. 669–670: Journal of Metals, 1950, Apr.). The double roasting of sinter containing a high percentage of lead concentrates raises the problem of removing the sheets of metallic lead formed in the wind boxes. A type of water-sealed wind box is described which obviates this, the metallic lead being granulated as it falls from the grate bars into the water bath from which it is readily removed without seriously interfering with the operation of the machine.—G. F.

Operating Practices at the Portsmouth Sinter Plant. G. B. Hunner. (Journal of Metals, 1950, vol. 188, Mar., pp. 471–473). A description is given of the operating practices at the Portsmouth Mine sinter plant of the Hanna Ore Co. and of the

principal changes put into effect to bring about an increase in average daily sinter production from 1123 tons in 1945 to 1658 tons in 1949.—G. F.

Fundamental Factors Influencing the Strength of Green and Burned Pellets Made from Fine Magnetite Ore Concentrates. P. A. Ilmoni and M. Tigerschiöld. (Jernkontorets Annaler, 1950, vol. 134, No. 4, pp. 135–170). [In Swedish]. The results of laboratory investigations carried out at the Mines Experiment Station of the Institute of Technology, Stockholm, are summarized. As raw material, magnetite ore concentrates were used exclusively. By improvements in balling it was possible to produce pellets of uniform spherical shape and dimensions. An apparatus has been constructed by which the crushing strength of the green pellets can be accurately measured. Methods of determining other properties of the pellets have also been improved. Series of trials with pellets made from concentrates of various fineness and with different water content were carried out. The strength of green and of dried and burned pellets was determined. To explain the results obtained in the balling operation, the hypothesis is offered that the compressing and binding of the green pellets is achieved by the combination of the capillary force of the added water and the mechanical force achieved by the balling operation. This hypothesis is supported by observations. In some cases a straight-line relationship has been found between the total grain area in a pellet and its crushing strength at optimum moisture content. In accordance with this theory, a new improved balling method was tried. It gave remarkably good results which will probably open up new possibilities of making pellets with considerably higher strength in the green as well as in the burned state. Preliminary experiments were made to study the mechanism of the drying and burning. An obvious parallel seems to exist between the properties of the green and burned pellets. Finally, a brief review is given of the practical experiments carried out at a small pilot pelletizing plant at Sandviken where very good pellets have been manufactured.—B. S. E.

Processing of Chvaletice Iron Ore. J. Hummel. (Hutnické Listy, 1950, vol. 5, June, pp. 247–248). [In Czech]. The Chvaletice iron ore has the following average composition: SiO_2 14·94%, Al_2O_3 0·19%, Fe_2O_3 10·54%, FeO 8·63% (total Fe 14–16%), MnO 21·53% (Mn 16·67%), CaO 7·48%, MgO 3·06%, P_2O_5 3·67% (1·6% P), S 8·37%, As 0·10%, CO_2 21·52%. Earlier industrial interest was restricted to the weathered top layers. The author suggested roasting of the ore with sulphate. Laboratory tests were carried out in an electrically heated glass tube at temperatures of 600° to 700° C., the atmosphere was air mixed with SO_2. About 85% of the manganese in the ore transforms fairly rapidly into $MnSO_4$ and can be extracted by water containing a small quantity of H_2SO_4; this process is considerably accelerated by heat. Experiments on a semi-production scale with rotary-drum furnaces with an adjustable tilting angle are proceeding. Since the process is exothermic little additional heat is necessary.—E. G.

Investigation of Calcined Pyrites Containing Zinc. Part II. Agglomeration of Calcined Pyrites by Sintering. S. Holewinski and W. Madej. (Prace Badawcze Glownego Instytutu Metalurgii i Odlewnictwa, 1950, No. 2, pp. 101–106). [In Polish]. Sintering tests on calcined pyrites containing zinc (iron 53·86%; zinc 5·38%) in a box (0·38 × 0·45 m.) have been carried out by a down-draught method to enable pyrites to be smelted in blast-furnaces. The difficulties in the sintering of calcined pyrites are mainly due to their small size (70% are smaller than 0·1 mm.), and their high melting point. Satisfactory results were obtained by adding 20% of slag from rotary kilns.—V. G.

Investigation on Calcined Pyrites Containing Zinc. Part III. Volatilization of Zinc during Sintering of Calcined Pyrites. M. Smialowski and J. Niewiadomski. (Prace Badawcze Glownego Instytutu Metalurgii i Odlewnictwa, 1950, No. 2, pp. 107–110). [In Polish]. A number of tests on removing zinc from calcined pyrites by down-draught sintering were made. The maximum removal of zinc obtained from a mixture containing about 30% of coke and about 20% of water, amounted to 73%. Under these conditions lead volatilized almost completely (up to 95%); sulphur was also removed (up to 80%), but the greater part of the arsenic remained in the agglomerate. Since the quantities of zinc and arsenic remaining in the sinter are too large it is not suitable for the blast-furnace process.—V. G.

FUEL—PREPARATION, PROPERTIES, AND USES

Developments in the Combustion of Solid Fuels: Application to Boilers and Metallurgical Furnaces. G. Whittingham. (Iron and Coal Trades Review, 1950, vol. 160, June 23, pp. 1371–1375). The author briefly reviews some of the problems encountered in the combustion of solid fuels in boilers and furnaces, and describes some recent developments in this field. The effect of variations in fuel quality encountered, particularly in recent years, is considered, and attention is given to the control of bonded deposits and to the combustion of low-grade fuels and pulverized fuel. Two new types of furnace, the cyclone and the down-jet, are briefly described.—G. F.

Bituminous Coal.—From Fuel to Chemical Raw Material. D. W. van Krevelen. (Chemisch Weekblad, 1950, vol. 46, Apr. 15, pp. 233–253). [In Dutch]. The author summarizes the present state of coal technology. Attention is devoted to genesis, petrology, and classification, and the technical and economic aspects of physical extraction, solvent extraction, hydrogenation, carbonization, total gasification, and controlled oxidation are discussed.—R. A. R.

Flotation of Coal Slimes with Xanthates. A. Götte. (Glückauf, 1950, vol. 86, June 24, pp. 493–498). Investigations in 1949 at the Institut für Aufbereitung, Kokerei und Brikettierung, Aachen, on the use of xanthates in the recovery of coal by flotation led to the following conclusions: (1) It is possible to float coal with xanthates alone; (2) this is possible because, when intact, the xanthate molecules have the required collecting effect, whilst the alcohol which is released when they split up acts as a frothing agent; (3) the quality of xanthate required depends upon (*a*) the number of carbon atoms in the xanthate molecule, and (*b*) the clay content of the coal; (4) xanthate appears to promote the flotation of the coarser coal particles; (5) excess xanthate does not cause 'over-oiliness' but the collection of particles whose surface is very low in combustible matter; (6) coal flotation is quite possible with pure alcohol as the sole addition agent; and (7) the use of xanthates with the ordinary tar oils does not affect the results and reduces the amount of xanthate required.

R. A. R.

A Study of the Operation of the Cyclone Washer and Its Application to Witbank Fine Coal. P. J. van der Walt. (Journal of the Chemical, Metallurgical and Mining Society of South Africa, 1950, vol. 51, Aug., pp. 19–103). The fine coal from the Witbank collieries produced a reasonably high proportion of coking coal and the possibility of recovering this by washing at 1·35–1·4 sp. gr. was studied. The reasons for selecting a cyclone washer are given together with an exhaustive account of investigations to determine its main operating characteristics. The results indicated that it should be possible to recover the coking fraction on a commercial scale.

B. A. R.

Concentration of the Coal Substance of Hungarian Lias Period Coals by the Flotation Method. J. Györki. (Bányászati és Kohászati Lapok, 1950, vol. 5, Sept., pp. 504–510). [In Hungarian]. The author describes in detail his experiments to find a suitable method of reducing the ash in high-ash Hungarian coals. He recommends that experimental work be started on reducing ash in lignite by flotation. Russian work is also mentioned.—E. G.

The Briquetting of Alberta Coals. W. A. Lang. (Canadian Mining and Metallurgical Bulletin, 1950, vol. 43, Sept., pp. 500–508). Processing methods and materials used in Canada and the U.S.A. are discussed. The development of briquetting in North America is outlined, and annual production figures for the past 40 years are given. U.S. output in 1947 was 3,100,000 tons, and in Alberta in 1949, 460,000 tons. Average processing cost for the latter was $1.45/ton, and the present market price is about $20/ton. Research into the processing of coal fines is outlined. There are 28 references.

T. E. D.

A South Wales Briquetting Works. (Coke and Gas, 1950, vol. 12, May, pp. 157–162). The Crown Fuel Works, Cardiff, has a daily capacity of 2600 tons of 25-lb. coal briquettes, which have a calorific value of 14,000 B.Th.U./lb., 18% volatile matter, and less than 9% ash. The proportion of pitch, with a softening point of 78–80° C., used as binder is 8%. Herbert Atritor pulverizers prepare the coal used as fuel in the vertical countercurrent dryers. Coal fines are recovered from the flue gas by Fraser and Chalmers 'Micron' precipitators, which are described. Graded coal, after

kneading with pitch, is flash heated and steam pressed at 1 ton/sq. in. into briquettes. Performance tests on the precipitators showed a precipitating efficiency of 97%, when treating 27,500 cu. ft./min., and collecting 3800 lb. of dust per hour. Apparatus for sampling the dust emission from the precipitator is described in detail.—T. E. D.

The Effect of Non-Caking Additions to Coal Blends on the Quality of Metallurgical Coke. F. Byrtus. (Prace Badawcze Glownego Instytutu Metalurgii i Odelwnictwa, 1950, No. 1, pp. 1–13). [In Polish]. The effect of changes in the proportions of the components of coke-oven charges on the properties of the coke has been examined. The addition of anthracite and up to 10% of semi-coke improved the quality of the coke, provided this was crushed to less than 1 mm. With a larger grain size the strength of coke decreased.—W. J. W.

Coke Oven Plant. (Metallurgia, 1950, vol. 42, July, pp. 72–74). An account is given of various modern coke-oven plants being erected and enlarged in Britain for the production of coke for blast-furnace use. The greatly increased cost of coal has made imperative the most efficient production of coke, gas, and by-products. The names of firms engaged in several types of plant construction are given.—J. J.

The First British By-product Coke Ovens. (Coke and Gas, 1950, vol. 12, Oct., pp. 371–376). An account is given, through the courtesy of M. D. Edlington, of the first battery of by-product coke ovens to be erected in this country. They were brought into operation by Pease and Partners at Bankfoot in Durham in 1882, and were designed by R. Dixon according to Carve's system, used in Belgium. Ovens of the same design, built on the same foundations, are still in use. T. E. D.

Coking Plant at Hirapur. (Coke and Gas, 1950, vol. 12, July, pp. 229–236). A new battery of 72 ovens, to carbonize 1600 tons of coal per day, has been built by Simon-Carves, Ltd., for the Indian Iron and Steel Co., Ltd., by local labour and from Indian materals. Two further batteries of the same size will be added. The coal and coke handling plant can move the eventual throughput of 6000 tons/day. The by-products plant is of the normal semi-direct type, but benzole is not recovered at present. Much of the plant is roofed, as a protection against monsoon weather.—T. E. D.

The Problem of Assessment of the Quality of Blast-Furnace Coke. F. Byrtus. (Hutnik, 1950, vol. 17, July–Aug., pp. 183–189). [In Polish]. The present interpretation of the results of the strength test for coke is criticized because it is based on a few size fractions and not on the complete screen analysis. It is suggested that during testing the coke should undergo breakdown similar to that produced during its passage through the blast-furnace, *i.e.*, size distribution after test should be similar to that of the same coke sampled from the tuyeres. K. Syskov's method for evaluating coke quality is described. It is claimed that this method not only takes into consideration the chemical analysis, reactivity to oxygen and carbon dioxide, and mechanical properties of the coke, but also the coke rate, uniformity of burden descent, and the intensity of blast-furnace driving. The quality of Polish metallurgical cokes is compared with those of the U.S.S.R.

Statistics of the Chemical and Physical Properties of Different Metallurgical Cokes Consumed in France in 1948 and 1949. C. G. Thibaut. (Publications de l'Institut de Recherches de la Sidérurgie, 1950, Part A, No. 22, Sept). Tables of properties, and graphs, are given covering European cokes used during the second half of 1948 and during 1949.—A. G.

Recommended Methods for the Sampling and Determination of Moisture in Coke, at Coke Ovens and Blast Furnaces. (Joint Coke Consultative Committee, Report of the South Yorkshire Coke Quality Panel, June, 1950). The Panel has found that the only satisfactory method of determining moisture is to determine the mean moisture in coke supplied from a particular source over a period. A new sampling scoop has been devised for collecting an increment of 10 lb. of large coke from flowing streams or a stopped belt, and a shovel method for sampling from side-door wagons is recommended. The mean moisture content of coke from a particular battery can be given accurately only for a bi-weekly or, for convenience, a weekly period. When sampling from wagons at a blast-furnace a month may be the shortest period necessary for obtaining sufficient increments to give the moisture content with sufficient accuracy. Methods for sub-dividing subsamples and making the moisture determination are specified in detail.—R. A. R.

TEMPERATURE MEASUREMENT AND CONTROL

Temperature Measurements in the Basic Arc Furnaces. C. B. Post and D. G. Schoffstall. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 75–89). *See* Journ. I. and S.I., 1950, vol. 166, Nov., p. 257.

Measuring Temperature of Molten Steel with Platinum Immersion Thermocouple. D. G. Harris. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 89–90). **The Immersion Thermocouple.** D. G. Harris. (Journal of Metals, 1950, vol. 188, Mar., pp. 469–470). The introduction in 1945 of immersion pyrometry at the Republic Steel Corporation's works, and its development to a routine practice is described. Its value for bath temperature control is pointed out.—R. A. R.

Applications of Temperature Measurements at Duquesne. G. E. Wagner. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 91–92). Control of temperature of the electric furnace steel at the Carnegie-Illinois Steel Corporation's works is attained by correlating three methods, namely, the optical pyrometer, the immersion thermocouple, and the judgment of the foreman melter. This short paper gives details of the construction of the platinum/platinum-rhodium immersion thermocouple which is used.—R. A. R.

Relative Accuracy and Cost of Using Immersion Thermocouples for Steel-Bath Temperature Control. J. G. Mravec. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 93–94: Journal of Metals, 1950, vol. 188, Mar., p. 470). The Timken Roller Bearing Company has found that temperature readings with the immersion thermocouple are more reliable and accurate than optical pyrometer readings, but the former are more expensive. A cost study of 3500 thermocouple readings showed that they cost 75 cents per reading for materials, plus 90 cents for constructing, maintaining, and checking.—R. A. R.

Temperature Measurement in the Electric Furnace at Atlas Steels Ltd. G. C. Olson. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 94–99). Both optical and thermocouple pyrometers are used by Atlas Steels, Ltd. The former are used for taking spoon-sample temperatures throughout a heat and are invaluable providing the instrument is in the hands of experienced and trained personnel; thermocouples are used at the end of refining before tapping. The quick-immersion thermocouple is a valuable aid to the melter in controlling temperatures at critical periods of the heat, but it requires more maintenance than the immersion Rayotube.—R. A. R.

Temperature Measurements and Fluidity Tests. G. A. Lilliequist. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 99–104). A double-spiral fluidity test mould with a large pouring cup fitted with a thermocouple has been used to make fluidity tests with a carbon steel and a 1·60% manganese steel. The latter had a higher fluidity than the former at the same pouring temperature. Steel deoxidized with aluminium and with high-carbon ferrotitanium had practically the same fluidity. The fluidity of steels with sulphur contents of 0·033%, 0·040%, and 0·060% increased slightly with increasing sulphur.—R. A. R.

Temperature Measurement and Temperature Control in Steel Foundries. J. F. B. Jackson. (American Institute of Mining and Metallurgical Engineers. Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 104–107). **The Immersion Thermocouple.** J. F. B. Jackson. (Journal of Metals, 1950, vol. 188, Mar., pp. 468B–469). It is shown that whereas 70% of British steel castings are produced in foundries employing quick-immersion thermocouple control of temperature, very few American foundries employ this method. The advantages of immersion pyrometry include simplicity in actual use, speed of temperature measurement, versatility in application, and accuracy of reading. On the other hand capital outlay for equipment and operational charges are both higher than for the disappearing-filament pyrometer.—R. A. R.

REFRACTORY MATERIALS

Refractories—Types, Properties and Service Behavior. R. P. Heuer and C. E. Grigsby. (Steel, 1950, vol. 126, Mar. 6, pp. 86–90, 110–117; Mar. 13, pp. 94–98, 125). The authors, of General Refractories Co., Philadelphia, discuss the types and forms of available refractories and their major characteristics and service behaviour. The application of refractories to furnaces for steel and non-ferrous processing is also covered.—A. M. F.

Research on Silica–Alumina Refractory Bricks Manufactured with Sardinian Raw Materials. F. Savioli. (Metallurgia Italiana, 1950, vol. 42, Aug.–Sept., pp. 305–309). The results obtained with refractory bricks manufactured with Laconi kaolin are discussed. A description is given of the raw materials, the methods of manufacture, and the system of quality control. The author suggests that the refractories obtained are commercially equal to those manufactured with the foreign raw materials normally used in the fabrication of bricks with an alumina content greater than 42%.—M. D. J. B.

The System CaO–MgO–FeO–SiO_2: I—Preliminary Data on the Join $CaSiO_3$–MgO–FeO. J. F. Schairer and E. F. Osborn. (Journal of the American Ceramic Society, 1950, vol. 33, May 1, pp. 160–167). An outline of the liquidus relations in the join is obtained through quenching experiments, magnesiowustites, melilites, olivines, merwinite, and pseudo-wollastonite appearing on the liquidus surface. The position of the join within the equilibrium tetrahedron of the quarternary system CaO–MgO–FeO–SiO_2 is elucidated. Stability relations of melilites are presented and refractive indices of the series are correlated with chemical composition.—E. C.

Behavior of Refractory Oxides and Metals, Alone and in Combination, *in Vacuo* at High Temperatures. P. D. Johnson. (Journal of the American Ceramic Society, 1950, vol. 33, May 1, pp. 168–171). Dry pressed samples of beryllia, magnesia, zirconia, thoria, molybdenum, and tungsten, also electrode carbon were fired *in vacuo* to 1500–2300° C. using 100° C. increments. Changes in bulk density, shrinkage, and weight are recorded, also the temperatures of surface contact instability for the 21 binary systems of the substances.—E. C.

Refractory Materials in the Construction of Boilers and Coke Ovens. H. H. Müller-Neuglück. (Glückauf, 1950, vol. 86, Aug. 5, pp. 640–643). Developments in improving the properties of refractories for boilers and coke ovens during the last ten years are discussed and the German standards are referred to. The need for a method of determining the sintering temperature of refractory mortars while under pressure between bricks is stressed.—R. A. R.

BLAST-FURNACE PRACTICE AND PRODUCTION OF PIG IRON

Swedish Blast Furnaces. C. G. Cedervall. (Jernkontorets Annaler, 1950, vol. 134, No. 8, pp. 343–460). [In Swedish]. The paper presents very detailed information on the design and operation of Swedish blast-furnaces. The development since 1913 is discussed in detail on the basis of a report in 1915. A comparison is made between Swedish and American and German coke-fired blast-furnaces. The development in Sweden is characterized by increased size of the furnaces, considerably lowered fuel consumption largely due to the use of sintered ores, and the substitution of coke for charcoal at a number of plants. The use of refined pig iron as a raw material for quality steel production is discussed.—B. S. E.

A Materials and Thermal Balance on a Modern Blast Furnace. J. Taylor, R. P. Towndrow, and J. D. Gilchrist. (Journal of the West of Scotland Iron and Steel Institute, 1948–49, vol. 56, pp. 126–144). An account is given of a week's trial carried out on No. 3 blast-furnace at Colvilles, Ltd., Clyde Iron Works, by a team of 36. The technique of sampling solid materials, slag, metal, and gas is described. with mention of the method of measuring slag and metal temperatures and the method of dealing with radiation and cooling losses. The errors in the materials balance are discussed, and the way of adjusting these so that they are not confused with those arising from thermal data is described. Two thermal balances are given in detail with percentage differences between heat evolved and heat absorbed of 1·7% and 0·9% respectively.—P. C. P.

Blast Furnace Tap Holes: Their Construction, Use and General Maintenance. D. Rist. (Iron and Coal Trades Review, 1950, vol. 160, May 12, pp. 1083–1086). The author discusses developments in the construction and maintenance of tap holes and describes the types of clay gun and clay mixture used, and the methods of opening and re-closing the holes. It is emphasized that the maintenance of a good tap hole is of vital importance to successful blast-furnace operation.—G. F.

Improving Blast-Furnace Practice. P. E. Cavanagh. (United Nations Conference on the Conservation and Utilization of Resources: Metal Progress, 1950, vol. 57, Apr., pp. 463–467). This article mentions the result of an enquiry into iron ore smelting. Blast-furnaces now tend to be too big for smelting efficiency, though efficient in manpower. Factors, such as oxygen enrichment, are considered, as related to reduction in the coke rate, and shown to have little effect; high top-pressure causes greatest improvement. The great advantages of a low-shaft oxygen furnace are outlined, though it is pointed out that it will not lead to much reduction in costs.—E. T. L.

Alternative Iron Smelting Processes. P. E. Cavanagh. (United Nations Conference on the Conservation and Utilization of Resources: Metal Progress, 1950, vol. 57, May, pp. 631–636). Results of consideration of 150 processes for producing iron from ores, bearing in mind fuel requirements, are given. There is a comparison of the economics of operation of large and small blast-furnaces, electric smelting and the sponge iron process, and the effect of market conditions, especially in Canada, is considered. Three other presses briefly dealt with are Tysland-Hole electric smelting, Wiberg-Söderfors sponge iron, and the brick-kiln sponge iron processes. It is concluded that alternatives to blast-furnaces are advantageous only on a small scale in some special conditions.

E. T. L.

TREATMENT AND USE OF SLAGS

Extraction of the Calcium Aluminate Slags Obtained from Blast-Furnaces Operating with Bauxite. E. Bogárdi. (Aluminium, 1950, vol. 2, Sept., pp. 222–223; Supplement to Banyaszati és Kohászati Lapok, 1950, vol. 5, Sept.). [In Hungarian]. The author describes his experiments on the extraction of Al_2O_3 from the calcium aluminate slags which form a by-product in the production of pig iron from bauxite ores in a small Hungarian blast-furnace.—E. G.

Blast-Furnace Slag Utilization Problems. R. Forbát. (Bányászati és Kohászati Lapok, 1950, vol 5, Sept., pp. 517–521). [In Hungarian]. The present utilization of blast-furnace slags in Hungary is unsatisfactory. Only a small percentage is used, the remainder being dumped. The chemical composition of the slags from the Diósgyör and Ózd ironworks are given and it is stated that their CaO/SiO_2 ratios are 1·25–1·3 and 1·1 respectively. At Diósgyör 20% and in Ózd 25% of the slag is utilized. Granulated slag produced by quenching in flowing water, contains 40–60% water, and the author draws attention to a Russian method, developed by Krilov and Krasenyikov [details not given] of producing granulated slag low in moisture. The author emphasizes the necessity of foreign expert advice on the correct methods of producing and using foamed slag. In the utilization of solidified lump slag, disintegration due to transformation of the bicalcium silicate into γ-bicalcium silicate presents a serious obstacle.—E. G.

Blast-Furnace Slag as Aggregate for Poured Concrete. A. Schneider-Arnoldi. (Stahl und Eisen, 1950, vol. 70, May 25, pp. 464–465). Should the demand arise, there are available many different types of aggregate for poured concrete and for the production of pre-formed blocks. Practical tests have shown that low-density concrete made with granulated blast-furnace slag and other slag products possesses extremely good properties in respect of density, thermal conductivity, water permeability, and heat retention.—J. P.

PRODUCTION OF STEEL

Reconstruction of a Large Integrated Steelworks in Normandy: Rebirth of the Société Métallurgique de Normandie after Invasion Damage. T. Bishop. (Iron and Coal Trades Review, 1950, vol. 160, June 30, pp. 1431–1441; vol. 161, July 7, pp. 25–36). A well-illustrated account is given of the general layout of the Mondeville works of the Société Métallurgique de Normandie and of the great damage which it suffered during the 1944 invasion. The programme of reconstruction and modernization now in progress is described, with special emphasis on the equipment being supplied from

Britain, and detailed descriptions are given of the new and reconstructed coke ovens, blast-furnaces, steelmaking units, power plant, rolling and wire mills, and associated equipment. The works is expected to be in full production by the beginning of 1952.—G. F.

Graphite Electrodes in Electric Steelmaking. J. Durand. (Journal du Four Electrique, 1949, vol. 58, Nov.–Dec., pp. 135–138). In this article the author traces the history of electric steelmaking and goes on to consider the part played by, and the properties of, the electrodes. Having detailed the necessary characteristics of such electrodes, he describes the manufacture, use, and performance of graphite electrodes.

J. A. L.

Materials Handling in the Electric-Furnace Shop. W. G. Nichol. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 5–18). It is estimated that of the labour cost involved in making electric furnace steel, material handling is responsible for about 50%. Means of reducing handling costs are discussed and descriptions of methods of moving scrap, refractory bricks, electrodes, ferro-alloys, and hot tops for ingot moulds in an electric furnace melting shop are given.—R. A. R.

Preparation of Scrap for Armco Electric Furnaces. C. F. Staley. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 18–24). The organization of the scrap yard at works of the Armco Steel Corporation is described and illustrated. There are two crane runways, each 825 ft. long, with two 7½-ton cranes on one and three 7½-ton cranes on the other. Under these runways are placed 16 alligator shears and a Harris hydraulic baler. There are also a 25-ton locomotive, a 40-ton diesel crane, and a crawler tractor.

R. A. R.

Handling Electric-Furnace Supplies at Timken. F. C. Wier. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 24–27). Materials are handled at the electric furnace plant of the Timken Roller Bearing Company by pneumatic-tyred vehicles powered with internal-combustion engines, such as fork-lift trucks and front-end shovel loaders. Refractory bricks are dealt with on pallets; bulk ferro-alloys are loaded into boxes by the shovel loaders, the boxes being designed for the fork-lift trucks. Sheets of electrolytic nickel are now cut to 9 in. × 27 in. and stacked to make fork-lift loads of about 4000 lb. Electrodes 18 in. in dia. × 72 in. long are supplied in unit loads of three.—R. A. R

Handling and Storage of Electrodes. J. J. Wyandt. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 28–30). Whereas it formerly required up to six men nearly 8 hr. to move electrodes from a railway box wagon to the wagon door, the electrodes are now palletized and arrive in gondola wagons from which they are unloaded by a front-lift truck in 1½ hr.—R. A. R.

Repairs and Maintenance Materials for Door Frames and Roof Rings on Electric-Arc Furnaces. J. P. Maloney. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 31–32). The roof ring of the 50-ton electric furnace of the Bethlehem Steel Company is 18 ft. 2½ in. in dia.; it is cooled by circulating water through a triangular channel formed by welding a flat strip to the inside of the ring; this channel provides four times the cooling area that was previously provided by a 1½-in. dia. pipe. The problem of maintenance of the ring is confined to the 2-ft. section above the charging door. Charging doors on all electric furnaces are water-cooled and brick-lined, while the work door is of brick-lined cast iron, but is not water-cooled.—R. A. R.

Inductive Stirring in Arc Furnaces. S. Fornander and F. Nilsson. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 46–69: Journal of Metals, 1950, vol. 188, Jan., pp. 22–29; Feb., pp. 256–256p). Results obtained in 18 months' operation of the ASEA (Swedish General Electric Co.) inductive stirrer on a 15-ton electric arc furnace are reported and critically examined. (The principles and the construction of the stirrer have already been described by L. Dreyfus and F. Nilsson, *see* Journ. I. and S.I., 1950, vol. 166, Sept., p. 94). Inductive stirring has no influence on carbon removal at high or medium carbon contents, but when producing low-carbon steel an exceedingly low level of carbon can be attained. In spite of this the oxygen in the steel and the FeO in the slag are lower than in conventional practice. Slagging-off is facilitated by the stirring and oxidizable impurities (*e.g.*, phosphorus and chromium) can be removed effectively. The reactions between steel and slag are accelerated, and the reducing time can be shortened. The differences in temperature and concentration in the bath during refining are smoothed out and heat transfer from the arc to the bath is facilitated. The stirring action is not violent; it is much less than that produced in a 5-ton high-frequency induction furnace operating on 1000 cycles. The installation of the stirrer is fully justified economically.

R. A. R.

Obtaining Uniform Bath Composition by Reladling. J. J. Green. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 69–70). To obtain a uniform bath composition in the electric furnaces at one American works, stirring with steel rabbles has been abandoned; instead, a portion of the metal is tapped out into a ladle and then poured back into the top of the furnace. This reduces the bath temperature by 100–200° F., which is advantageous as the bath is very hot after reduction of chromium from the slag. It has been easier to keep the composition of the stainless steel produced within the specified limits since this reladling practice was introduced.—R. A. R.

Stirring with Neutral Gas. J. H. Kennedy. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 71–72). The efficiency of reladling to obtain uniform composition of the bath, as reported by J. J. Green (*see* preceding abstract) is confirmed by tests at the Republic Steel Corporation where the injection of argon through six ¼-in. pipes at 40 lb./sq. in. has been tried. Another experiment was the addition of four 10-lb. tins of metallic calcium: this caused a very vigorous action for a short time, but there was still some variation in analysis.—R. A. R.

Optimelt Automatic Recalibrating Control for Arc-Furnace Regulators. R. M. Bayle. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 129–138: Journal of Metals, 1949, vol. 1, Dec., pp. 14–19). There is an optimum value of arc current that produces maximum arc kilowatts for a particular secondary voltage, but at the time of greatest power input to an electric furnace, *i.e.*, during melting, the instrument readings fluctuate so violently that it is impossible manually to set current-adjusting rheostats in each of the three phases to secure anything approaching a uniformly efficient condition. The Optimelt control of the Westinghouse Electric Corp. is a supplementary supervisory equipment which automatically provides the above optimum current conditions. It is available in two forms, one to fit to existing equipment, and one for completely new arc furnace installations. Both types of controller are illustrated and described, and simplified circuit diagrams are given. Some advantages expected from this control are: (1) Highest values of power factor corresponding to maximum power-input requirements; (2) less kilowatt-hours per heat; (3) less time per heat; (4) protection against excessive carbon pick-up; (5) longer life of refractories; (6) reduced consumption of electrodes; and (7) elimination of human error and the time required to make manual current adjustments. This equipment has been installed by the Republic Steel Corp. and results obtained are reported by L. A. Wynd (*see* next abstract).—R. A. R.

Results of Optimum Current Control on Electric Furnaces. L. A. Wynd. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 139–155: Journal of Metals, 1950, vol. 188, Jan., pp. 17–21). Some results obtained with the Optimelt arc furnace automatic regulator on a 70-ton Héroult furnace of the Republic Steel Corp. are reported. This regulator has been described by R. M. Bayle (*see* preceding abstract). The transformer in question has secondary tappings of 124, 159, 200, 230, and 275 V. and the new control is used on the three highest voltages. Charts compare current consumptions per ton, tons per furnace hour, heat times, and average delays per heat on similar furnaces with

and without the automatic control. Considerable advantages with this control are indicated. In the discussion, T. S. Winters describes an automatic arc power 'maximizer' which is a power-measuring servomechanism for the automatic independent positioning of each arc-furnace electrode for optimum current and maximum arc power, regardless of changes in voltage, tap position, reactance, or other variable.

Effect of Large Transformers on Rate of Production. R. J. McCurdy. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 155–156). Melting practice with an electric arc furnace equipped with a 20,000-kVA. transformer is compared with that of similar furnaces supplied by 12,000-kVA. transformers. The high-voltage tap (No. 5 of the large transformer) is used only for 15–20 min. after the electrodes reach their lowest point, the long arc rapidly melting the surrounding scrap. With heavy scrap on the bottom, No. 4 tap is employed when the electrodes are still close to the bottom. Some advantages of the larger transformer are: (1) Production is 1 ton/hr. greater; (2) kilowatt-hours per ton are reduced; (3) less electrode breakage; (4) lining and roof life is not shortened; (5) lighter scrap can be used; and (6) there is less possibility of the electrodes boring into the bottom.—R. A. R.

Effect of Large Transformers on Rate of Production. W. M. Patterson. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 157–158). The operation of a 75-ton arc furnace with a 16,000-kVA. transformer with a highest tap voltage of 350 is discussed. Production of 12 to 20 tons/hr. of rimming steel is easily maintained but wear on walls and roof is severe, particularly of the bricks in the electrode circles. With charges with a fairly level top the walls and roof are quickly worn when the 350-V. top is applied, but with more uneven scrap, 350 V. is quite practical during melting.—R. A. R.

Transformer Trends. S. J. Myford. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 158–164). Trends in the development of electric furnace transformers at Copperweld Steel Company's works from 1939 to 1948 are discussed. In this period the scrap became lighter and lighter. By top-charging instead of door charging and by increasing the furnace height the scrap-holding capacity has been increased by 40%. It was thought that the increased height of the furnace would increase the kilowatt-hours per ton and the electrode consumption, but in practice the reverse has proved true, and roof life has markedly increased.—R. A. R.

Good Joint-Making Practice for Electrode Columns. W. H. Herbert. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 164–168). New gauges for checking taper-thread electrodes and connecting pins are described and illustrated. A procedure for joining graphite electrodes is given in detail; this stresses the cleaning of the two ends before screwing up, and applying the recommended torque for the particular size. For taper-threaded graphite electrodes the torques for 6, 7, 8, 9, 10, 12, 14, 17, 18, and 20 in. electrodes are 100, 150, 200, 300, 425, 600, 1200, 1750, 2000, and 2400 ft.-lb. respectively.—R. A. R.

Improved Method of Handling and Shipping Graphite Electrodes. J. A. Shaw. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 169–176). The improved methods of packing, loading, and unloading electrodes for rail transport, developed by the International Graphite and Electrode Corp., are described and illustrated.—R. A. R.

Thermal Gradients and Strains during Rapid Cooling of Graphite Electrodes. H. G. MacPherson. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 177–184). **Graphite Electrodes.** H. G. MacPherson. (Journal of Metals, 1950, vol. 188, Mar., pp. 481–484). The strains set up by thermal shock when an 18-in. electrode is lifted rapidly out of a furnace with a bath temperature of 1650° C. and cooled by radiation to atmosphere are calculated and curves for the temperature and stress distribution across a diameter of the electrode are shown. Thermal expansion and thermal conductivity curves for Acheson graphite electrodes are also shown.—R. A. R.

Tightening Electrodes. J. H. Eisaman. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 184–186) The advantages and disadvantages of assembling electrodes for arc furnaces 'off the furnace' are pointed out and a pneumatic device for tightening screwed joints in electrodes is described. The device consists of a 5-in. steel plate with six holes in each of which an electrode can be wedged; a band, with a rigid arm, surrounds the electrode and to this a reproducible predetermined force can be applied by an air cylinder. A graph shows the relation between the joint resistance in ohms and the applied torque in foot-pounds. For a 20-in. electrode the torque recommended is 2400 ft.-lb.

R. A. R.

Automatic Electrode Slipping. S. J. Myford. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 186–188). A description is given of a pneumatic holder for 18-in. electrodes for electric furnaces. Springs on one side of the piston in an air cylinder supply the energy through a system of linked bars to the plate or block which forces the electrode against the contact surface of the holder. Compressed air, controlled from the melting-shop floor, applied to the other side of the piston compresses the springs to release the holder. There is less risk of accident because time spent on the furnace roof to slip electrodes is eliminated entirely.—R. A. R.

Deoxidation of Low-Carbon Rimming and Semikilled Steels Melted in 70-Ton Basic Electric Furnaces. W. W. Black. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 188–190). When making low-carbon rimming steel in a 70-ton arc furnace, difficulty was encountered in securing a uniform mould action; the degree of oxidation varied widely, making aluminium additions to the ladle and moulds very critical. A statistical investigation of the variables likely to affect this showed that best mould action was obtained when: (1) there was 35 to 40% FeO in the slag; (2) the tapping temperature was 2880° to 2900° F.; and (3) the amount by which the carbon was reduced between 'all melted' and tapping very slightly influenced mould action, the action being better with the least reduction.

R. A. R.

Manufacture of Rimming Steel in Small Electric Furnaces. A. J. Texter. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 190–191). The production of low-carbon rimming steel in an Héroult electric furnace 9 ft. in dia. is described. Average figures for 290 consecutive heats are: (1) Production about 8 tons/heat and 3·49 tons/hr. (2) power/ton 543 kW., (3) time/heat, tap to tap 1 hr. 50 min., and (4) oxygen consumption per ton of steel 185 cu. ft.

R. A. R.

Deoxidation Practice on Rimmed and Semikilled Steels by Steel Company of Canada. A. K. Moore. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 192–194). Deoxidation practices in a 70-ton electric furnace and a 185-ton open-hearth furnace are compared. In general, the former requires less aluminium and less silicon for comparable deoxidation.—R. A. R.

Deoxidation Practice on Rimmed and Semikilled Steels. C. H. Weimer. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 195–196). Deoxidation practice with 40 and 50-ton electric steel furnaces melting cold scrap charges is described. With high sulphur and high FeO due to poor scrap, a manganese reboil plus an addition of lime was made and then most of the slag was flushed off. With heats of semi-killed steel, when FeO in the slag was high, 15% silicon pig iron was added, but with FeO below 15% at tapping, ferromanganese was put in the ladle; the pouring temperature was about 2860° to 2880° F.—R. A. R.

Deoxidation Practice for Single-Slag Electric Furnace Rimming Steel. J. R. Atkinson. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, p. 196). In the electric furnaces of Dominion Foundries and Steel, Ltd., the FeO in the tapping slag is in the 18–24% range. Deoxidation is accomplished by the addition of manganese, titanium, and

aluminium to the ladle during tapping of the first half of the metal.—R. A. R.

Quality of Oxygen-Decarbonized Acid Electric Steel. C. A. Faist and C. Wyman. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 219–240). *See* Journ. I. and S.I., 1950, vol. 166, Oct., p. 156.

Techniques Employed in Using Oxygen to Make Carbon Steel for Castings. C. C. Spencer. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 241–243). Three methods of using oxygen which have been used to advantage by The Electric Steel Castings Company, Indianapolis, are described. In the first, a carbon content of 0·3–0·40% is obtained at melt-down without adding ore by injecting oxygen with a lance while the electrodes are raised. The end of the lance is protected by splitting two 9-in. insulating bricks and drilling the four pieces lengthways so that they will cover 36 in. of the lance. In the second, some ore is used early to oxidize the silicon and manganese and oxygen is then injected to bring down the carbon; when there is a vigorous boil, oxygen is added for only 40 sec. to 1½ min. at 70 lb./sq. in. through a ⅜-in. pipe; good control of carbon, manganese, silicon, and aluminium is obtained. The third method consists of injecting oxygen as soon as there is a pool of metal under the electrodes and the scrap is hot; after the centre of the charge has melted the bath is sufficiently oxidized to create a boil. On four heats the time saved by this method was 15 to 29 min. on each.—R. A. R.

Ore, Oxygen and Slag Volume. J. W. Juppenlatz. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 244–246). Experience has shown that injecting oxygen into acid electric furnace steel to oxidize the impurities greatly decreases the volume of slag as compared with the practice of ore additions; there is also a better recovery of alloying elements when there is less slag, and the refractory lining lasts longer.—R. A. R.

High Chromium Steel in the Acid Arc Furnace. D. C. Hilty. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 246–252). *See* Journ. I. and S.I., 1950, vol. 166, Dec., p. 367.

Use of Oxygen in Production of High-Chromium Steels. C. J. Jernstrom. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 252–256). Oxygen injection practice with an acid-lined direct-arc furnace producing about 1500 lb. of high-chromium and 18/8 stainless steels is described. Oxygen is injected through a ¼-in. pipe at 60 lb./sq. in., the flow being 150 to 200 cu. ft./min. After 30 sec. to 1 min. the carbon flame becomes visible. After 2 to 2¼ min. the flame drops, injection is stopped, the predetermined amount of ferrochromium is added and the heat is tapped. To bring the carbon content above 0·15% before injecting oxygen when very low carbon scrap is melted, about 20 to 40 lb. of sweepings from the casting grinding room are added; these contain 10% of nickel, 9% of chromium, and 2% of carbon. When blowing with oxygen it is possible to include 50 to 150 lb. of oily turnings from stainless steel in the charge, as well as to use charges containing 75 to 90% scrap. The cost of materials charged has thus been brought down from about $200 to about $140 per short ton.—R. A. R.

Use of Oxygen in Melting Stainless Steel. W. T. Bryan, (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 256–266). Data are presented on the use of oxygen in both experimental and production heats when making 20/25 chromium–nickel heats in acid electric furnaces and their application to the production of 18/8 stainless steel. It was found necessary: (1) To determine the bath temperature before injecting oxygen; this should be at least 3150° F.; (2) to maintain a constant flow of oxygen and control the amount by time of injection; (3) to make final chromium additions on the basis of the quantity of oxygen used and the carbon range worked in the heat. The quantity of nickel in a heat affects the reactions, and this must be allowed for. The use of aluminium to reduce chromium from the slag in acid furnaces is not practical.—R. A. R.

Effect of Melting Practice on Hydrogen. S. F. Carter. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 267–301: Journal of Metals, 1950, vol. 188, Jan., pp. 30–40; Feb., pp. 245–255). An extensive investigation has been made on the effect of many variables in electric furnace steel practice on the quantity of hydrogen in the finished steel. A system of sealing samples in glass and transporting them a long distance to the Batelle Memorial Institute for analysis is described in detail. Certain phases of furnace practice were deliberately exaggerated to emphasize their individual effects. The general conclusions were: (1) Composition of the metal had a great effect; low carbon steel finished with hydrogen contents of 0·11 to 0·35 rel. vol. (relative volume) in spite of attempts to get more; on the other hand, there was four times as much hydrogen in austenitic chromium–nickel steel under better conditions. (2) Carbon steels began to give off hydrogen at room temperature within a few minutes of casting, whilst several austenitic steels did not give off any hydrogen at room temperature and required a long time at high temperature. (3) Humidity of the atmosphere can have a marked influence on the hydrogen content of the steel, but with some compositions and sequences of operations hydrogen absorption can be resisted so that some heats with a continuous spray of water finished with less hydrogen than other heats exposed only to atmospheric moisture. (4) The final hydrogen is made up of five fractions: (*a*) hydrogen from the metal charged ± (*b*) that absorbed or lost in melting − (*c*) that eliminated by the boil + (*d*) that absorbed in the furnace during finishing + (*e*) that absorbed when tapping into ladle. These phases are not equally important. (5) Steel has maximum affinity for hydrogen when ready to tap so that improperly dried ladles and spouts have much more effect than conditions in the furnace. (6) Acid electric furnace practice to produce carbon steel very low in hydrogen consists of an oxidizing melt, a violent boil, an oxidizing final slag, and a short deoxidation time. (7) Chromium seems to be the alloying element which produces the affinity for hydrogen and nickel seems to cause strong retention of the hydrogen.—R. A. R.

Flushing Molten Steel with Neutral Gases. C. E. Sims. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 302–313). Tests are reported the object of which was to obtain data on the efficiency of flushing molten steel with argon, helium, and nitrogen in order to remove hydrogen. The amount of hydrogen removed per unit volume of the flushing gas was about the same as that removed by CO during the carbon boil and this amount was proportional to the hydrogen content. The efficiency of the treatment appeared to be independent of the composition of the steel and about the same for argon, helium, and nitrogen. With nitrogen flushing, some of it was dissolved by the steel. A correlation was found between hydrogen content and the amount of porosity in ingots for one set of conditions. Under the same conditions, the porosity was decreased after removing part of the hydrogen by flushing.—R. A. R.

Oxygen Lancing in the Arc Furnace: British Techniques for Refining Steel in Electric Furnaces. T. Bishop. (Iron and Coal Trades Review, 1950, vol. 160, June 16, pp. 1331–1333). The techniques used by two British firms for oxygen lancing in arc furnaces are described. At Brymbo Steelworks Ltd., the practice is used in 29-ton furnaces producing low-carbon and alloy steels, and it appreciably reduces the refining time and power consumption compared with the practice of using ore to promote the carbon boil. Edgar Allen and Co. Ltd. use the oxygen lance in the manufacture of 18/8 stainless steel in a 4-ton electric furnace and find distinct advantages in that, compared with ore practice, the oxygen lance reduces chromium loss, enables more stainless scrap to be used, and decreases refining times.—G. F.

The Use of Oxygen in Steelmaking. (Metallurgia, 1950, vol. 42, June, pp. 29–32). The possibilities of using oxygen in the iron and steel industry have been recently carefully examined. It is thought that the production of 'tonnage oxygen' of 90 to 95% purity would bring its cost down to attractive levels. In some cases, difficulties in practical application have been encountered. In the blast-furnace, 30% is considered the maximum possible oxygen enrichment, and it is doubtful whether this relatively small proportion

would make its use worthwhile. In the open-hearth furnace, risks of roof damage are great except in all cold metal practice. Its use is, however, of great practical importance in bottom and side-blown converters, when hotter metal can be produced and/or more scrap used. The most successful applications have been in electric arc furnaces producing respectively low carbon silicon steel for electrical sheet and stainless steel from the hitherto unusable stainless steel scrap. In the silicon steel furnace oxygen lancing of the bath has been found to produce rapid and controllable carbon elimination. In the stainless steel furnace, the very high temperatures obtained by oxygen lancing result in the preferential oxidation of carbon rather than chromium, resulting in the production of high quality, low-carbon stainless steel from stainless scrap. J. H. E. J.

Results of Investigations on Metal–Slag Reactions. W. Oelsen. (Mitteilungen aus dem Max-Planck-Institut für Eisenforschung: Zeitschrift für Erzbergbau und Metallhüttenwesen, 1949, vol. 2, Nov., pp. 328–334; Dec., pp. 368–371). The author reviews the results of 15 years' research on slag–metal reactions at the Kaiser-Wilhelm-Institut für Eisenforschung, most of which have been published. With regard to aluminium and sulphide slags, when this element is added to a melt of high-carbon iron covered by an iron sulphide slag, the first additions form an aluminium sulphide slag on top of the first slag. Further additions take up all the sulphur from the iron sulphide slag until all the slag consists of aluminium sulphide; still more additions increase the aluminium in the iron and decrease the sulphur in the iron. If there is copper in the melt, this is taken up much more strongly by the aluminium sulphide than by the iron sulphide.—R. A. R.

Evaluation of *p*H Measurements with Regard to the Basicity of Metallurgical Slag. C. W. Sherman and N. J. Grant. (Transactions of the American Institute of Mining and Metallurgical Engineers, 1949, vol. 185, pp. 898–903; Journal of Metals, 1949, vol. 1, Nov.). The determined *p*H values of aqueous solutions extracted from powdered slags are presented for a wide range of slag compositions varying from neutral bisilicate blast-furnace slags to highly basic open-hearth slags. The *p*H value is found to vary with 'excess CaO' in the sample, which is defined as the amount of CaO remaining after subtracting the total acids from the total bases. A close relationship is also shown between *p*H value and the desulphurizing ratio in the case of MnO-free blast-furnace slags. G. F.

Melting Points in the System TiO_2–CaO–MgO–Al_2O_3. H. Sigurdson and S. S. Cole. (Transactions of the American Institute of Mining and Metallurgical Engineers, 1949, vol. 185, pp. 905–908; Journal of Metals, 1949, vol. 1, Dec.). The purpose of the study described was to establish basic information on low-melting-point titanate mixtures and to provide useful data for smelting titaniferous ores, the work being restricted to the zones including crystalline phases present in the slags. The quaternary system is represented graphically by a tetrahedron, the limits explored being bounded by $CaO.TiO_2$–$MgO.TiO_2$–TiO_2 in the base plane with additions of a fourth component $Al_2O_3.TiO_2$ in 10% increments up to 40%. In the base plane it is established that a eutectic exists at 1360° C. between $CaO.TiO_2$ (40%) and $MgO.2TiO_2$ (60%), but melting points at about 1375° C. are obtained with mixtures much higher in TiO_2 than this combination. Additions of $Al_2O_3.TiO_2$ up to 30% are shown to have no adverse effect on the melting points, but an addition of 40% causes a sharp decrease in the zone of fluid melts.—G. F.

Laboratory Smelting of Titaniferous Ores. D. L. Armant and S. S. Cole. (Transactions of the American Institute of Mining and Metallurgical Engineers, 1949, vol. 185, pp. 909–913; Journal of Metals, 1949, vol. 1, Dec.). In the smelting of titaniferous ores, the primary objective is the production of a fluid high-titanium slag substantially free from iron and low in trivalent titanium. A description is given of laboratory experiments on induction-furnace and arc-furnace smelting of MacIntyre and Baie St. Paul ilmenite ores containing 38–44% TiO_2. Melting-point data supplied by H. Sigurdson and S. S. Cole (*see* preceding abstract) are applied, and it is shown that a fluid slag with 65–69% TiO_2 can be produced avoiding appreciable amounts of trivalent titanium. The slag requires 5–11% MgO and 9–13% CaO in order to have the desired fluidity.—G. F.

Petrology of High Titanium Slags. C. H. Moore, jun., and H. Sigurdson. (Transactions of the American Institute of Mining and Metallurgical Engineers, 1949, vol. 185, pp. 914–919; Journal of Metals, 1949, vol 1, Dec.). A description is given of experiments made to study the course of reaction and crystallization in high-titanium slags of the system CaO–MgO–TiO_2–Al_2O_3–SiO_2, resulting from the smelting of Baie St. Paul ilmenite ore with a suitable flux in a 50-lb. arc furnace. Micrographs of slag samples taken at 10-min. intervals are shown, and a description of the course of reduction and of the mineral phases occurring is given.—G. F.

The Graphical Representation of Metallurgical Equilibria. C. J. Osborn. (Transactions of the American Institute of Mining and Metallurgical Engineers, 1950, vol. 188, pp. 600–607; Journal of Metals, 1950, Mar.). A complication of curves is presented showing the dependence on temperature of the free energies of formation of metallurgically important oxides, sulphides, chlorides, carbonates, and sulphates. The curves are drawn according to the latest available thermodynamic data, and offer a simple method of deriving practical metallurgical information. The paper gives a more complete and up-to-date compilation of free-energy curves than any hitherto published.—G. F.

New Ingot Mould Coatings. J. J. Golden. (Industrial Heating, 1950, vol. 17, Sept., pp. 1538–1546). An account is given of tests at the Gary works of Carnegie-Illinois Steel Corp. on new ingot mould coatings, the purpose being to obtain an inexpensive, fumeless material approaching tar in performance. The two most promising materials to date are a colloidal graphite solution which resembles a heavy black paint, and a non-inflammable paste consisting of flake aluminium and binder. The sprayer for applying thin coatings to the moulds is described.—J. A. L.

The Freezing of Continuously Cast Steel. H. Krainer and B. Tarmann. (Metal Treatment and Drop Forging, 1950, vol. 17, Spring Issue, pp. 3–10). This is an English translation of a paper which appeared in Stahl und Eisen, 1949, vol. 69, Nov. 10, pp. 813–819 (*see* Journ. I. and S.I., 1950, vol. 165, May, p. 109). There are some comments by J. Savage to the effect that the authors over-emphasize the importance of mould design and that the real problem is to devise methods of dealing much more rapidly with large quantities of steel so as to be able to cast on a commercial scale in competition with existing open-hearth methods.—R. A. R.

Sulphides in Alloy Steels. J. Bardin and Ju. Lukashevich-Dubanowa. (Izvestiya Akademii Nauk, U.S.S.R.: Hutnik, 1950, vol. 17, July–Aug., pp. 261–263). [Translated from Russian into Polish]. On the basis of analytical and microscopical examinations it is postulated that in alloy steels sulphides exist in a combined form comprising sulphides of alloying elements which are not always present as inclusions in metal. The carbides of titanium, vanadium, and aluminium and probably to a certain extent of chromium, molybdenum, and tungsten are able to retain a considerable amount of sulphides in solution at high temperatures. These sulphides form stable compounds with carbides and on cooling remain in solution or are only partly precipitated. When sulphides on cooling remain completely or partly in carbides it is possible to assume that sulphur has no deleterious effect on the metal. The presence of slightly soluble (in HCl) sulphides should also be less harmful, since they could not become a nucleus of oxidation processes usually caused by ferrous and manganese sulphides. The beneficial influence of chromium, molybdenum, tungsten, vanadium, aluminium, and in particular of titanium, on steel is explained not so much by their deoxidizing power and in some cases nitrogen-fixing properties as by the transfer of sulphides into combined sulphide-carbides which are less harmful than simple sulphides.—V. G.

Further Investigations on the Solidification of Rimming Steel Ingots. A. Hultgren, G. Phragmén, S. Wohlfahrt, and J. E. Östberg. (Jernkontorets Annaler, 1950, vol. 134, No. 5, pp. 189–221). [In Swedish]. Blowhole distribution and primary structure are governed by the pressure under which the rimming steel solidifies as the evolution of gas is reduced with increased pressure. When the pressure applied exceeds a certain minimum, no gas evolution takes place and the structure shows the characteristics of a killed steel. On experimenting with a steel with 0·12% C and 0·48% Mn an additional air pressure of 10–15 atm. was sufficient to prevent all gas evolution during solidification. Under an additional pressure of 6·5 atm. a semi-killed ingot (0·07% C, 0·24% Mn) was obtained, and under 3–4 atm. a rising ingot (0·08% C, 0·33% Mn). The application of a lid, with a

casting orifice, appeared to reduce gas evolution during rimming to some extent; the steel may therefore rise during this period. A similar effect was probably produced by a refractory lining at the top of the mould. Rimming steel with about 0·40% C differs from such steel of lower carbon contents in that a good rimming action seems more difficult to produce, although not impossible. The inner part of the rim zone tends to develop a globular freezing structure. On slow pouring, the outer part of that zone is less blown than on rapid pouring. More large non-metallic inclusions appear in rimming steel ingots with 0·08–0·21% C in the lower than in the upper part of the core. Rim channels in rimming steel ingots were in two cases found to show continuity from a depth beneath the ingot surface of about 30 mm. to the intermediate blowholes, but not beyond this depth.—B. S. E.

Distribution of Inclusions in Forged Steel in Relation to the Macro- and Micrographic Structures. P. Cattier, C. Dubois, J. Bleton, and P. Bastien. (Revue de Métallurgie, Mémoires, 1950, vol. 47, Aug., pp. 619–636). A semi-hard nickel–chromium steel (C 0·3%, Ni 1·6%, Cr 0·6%) was made in an electric furnace using four different processes, (*a*) basic, (*b*) duplex, (*c*) normal acid, and (*d*) acid with a high casting temperature. Chemical and structural heterogeneity were examined and inclusions classified according to their nature. Iodine attack was used to reveal structure near the surface of the ingot. Carbon and sulphur play a passive rôle in segregation and obey the laws of freezing of solutions. Oxygen segregation can be related to the macroscopic distribution of silicates and to a negative segregation of carbon in the centre of the ingot base. The distribution of sulphide and silicate inclusions after forging is described. In the cast ingot most silicates are in the centre of the base, a fact partly explained by Benedick's hypothesis postulating the initiation of crystallization by liquid inclusions. Sulphides are mainly in the top centre, but spread as the sulphur content increases. This is explained by local supersaturation at the crystallization front.—A. G.

FOUNDRY PRACTICE

On the Importance of Work Preparation in Foundries. J. Přibyl. (Hutnické Listy, 1950, vol. 5, Oct., pp. 418–421). [In Czech]. The problems relating to planning and preparation of work in Czech foundries are discussed. The tasks of the departments are investigated and the importance of clear definition of and strict adherence to these tasks is stressed. To ensure delivery at the specified date, the author recommends application of penalty payments by the foundry personnel concerned if their respective tasks are not completed in time.—E. G.

Job Preparation in Foundries. J. Mladějovský. (Hutnické Listy, 1950, vol. 5, June, Supplement No. 2, pp. 69–73). [In Czech]. Detailed preparation for operations in the foundry are discussed. The author emphasizes the importance of standardization of materials, tools, and equipment, and the planning of the separate operations.—E. G.

Trends in the Development of Polish Foundries. G. Kniaginin. (Hutnik, 1950, vol. 17, July–Aug., pp. 204–207). [In Polish]. The present state of the Polish foundry industry is reviewed and the necessary improvements are advocated.

V. G.

Future of the Founding Industry. F. W. Rowe. (Institute of British Foundrymen: Foundry Trade Journal, 1950, vol. 88, May 4, pp. 475–478). Although advances have been complicated by prevailing conditions, the major outstanding progress which is being made and will continue for some time is in the improvement in conditions for the foundry worker. Considerations which require the greatest thought are ventilation, lighting, heating, and washing accommodation. Changes are necessary for progress, though careful study must be made before any change is introduced and the correct use of accurately compiled records will often point out the correct path to take. Progress is possible in such fields as sand bonding and metallurgical and melting processes, but the future is entirely regulated by the number and calibre of the men entering the industry.—J. E. W.

Grey Ironfounding. (Anglo-American Council on Productivity, 1950, Productivity Team Report on Grey Ironfounding). A report is given of a visit to the U.S.A. in 1950 of a productivity team representing the grey ironfounding industry of the United Kingdom. A full description is included of what the team saw, with sections on such items as metal melting and control, moulding practice, core-making practice, core-box equipment, and costing. In the part dealing with the opinions and findings of the team, mention is made of the problems facing the British industry, such as insufficiency of juvenile, skilled, and unskilled labour, lack of incentives for labour, management, and capital, shortages of plant, etc. Some of the reasons given for high productivity are that the people there are definitely production minded, the greater use of power, mechanical aids, and unskilled labour, the high degree of co-operation between workers and executives, the fierce competition between foundries, the larger scale of operations, with an abundant supply of new plant and greater borrowing facilities from the banks, careful time study, greater human energy (causes of which include climatic conditions, better food, the pioneering spirit which still survives, and the influx of the more enterprising members of European races), the fear of unemployment, and training and education which have a greater bias towards industry.—P. C. P.

Preheating and Flow Measurement of Air for a 3-ft. Diameter Cupola. P. G. Maceraudi. (Metallurgia Italiana, 1950, vol. 42, Oct., pp. 347–351). The object of this investigation was to find the actual behaviour of the air passing into a cupola both under normal, uncontrolled conditions and under carefully balanced ones. The author also studied the behaviour of the tuyeres while the cupola was in operation and the effect of balanced flow. The preheating of air is also examined but the investigation was limited to low temperatures (about 150° C.). The complete utilization of the latent heat of the gases had to be abandoned as the experimental apparatus required was too costly. Whilst it is obvious that efficiency increases with the temperature of the preheated air, it is interesting to note that even with low temperatures the tests were so satisfactory as to pay for the experiment.

M. D. J. B.

Supply of Additional Combustion Air to Cupolas. (Bányászati és Kohászati Lapok, 1950, vol. 5, Oct.: Öntöde, p. 240). [In Hungarian]. Additional air for combustion to increase the temperature of the melt was fed to a cupola in which small coke was the fuel. The cupola was 700 mm. in dia. and the normal combustion air was fed through ducts about 150 mm. wide and 40 mm. high at 500–600 mm. above the hearth. Additional air was supplied through eight ducts 50 mm. in dia. at a higher level and with a correspondingly lower pressure. Melting was effected at a pressure of 400 to 600 mm. of water. The increase in the temperature of the melt was due to the high speed of the additional air which penetrated radially into the furnace space. The advantages claimed are: The tuyeres in the melting zone are not affected by slag in spite of the higher temperature, melting is more uniform, which increases the life of lining, melting time is reduced, coke consumption is also reduced by at least 7%, and the quality of the castings is improved.—E. G.

Desulphurization Experiments with Calcium Aluminate Slag in Cupolas. F. Varga. (Bányászati és Kohászati Lapok, 1950, vol. 5, Oct.: Öntöde, pp. 228–232). [In Hungarian]. Previous trials have shown that an alumina slag is a more powerful desulphurizer than a silica slag. The experiments now reported were to determine to what extent the Al_2O_3 content of cupola slag can be increased and the effect of the increase on the desulphurization of the liquid metal. The experiments were carried out in a cupola 600 mm. in dia., melting 1·8 tons/hr. The results of 30 experimental melts with various CaO/Al_2O_3 ratios in the slag are given in tables and graphs. Best results were obtained with a CaO/Al_2O_3 ratio of 5 : 1.—E. G.

A New Method for Improving the Quality of Cast Iron Produced in Cupolas. N. A. Barinov and V. I. Volkov. (Bányászati és Kohászati Lapok, 1950, vol. 5, Oct.: Öntöde pp. 224–227). [In Hungarian]. The authors report on experiments on the improvement of the quality of iron produced in cupolas. The effect of replacing steel scrap in the charge with white pig iron and partly replacing grey iron with white iron was examined. The conclusions were: (1) If a suitable charge composition is applied, cast iron with finely distributed graphite and a structure of sorbite, fine pearlite, and ferrite but no free cementite is obtained. (2) The addition of white pig iron improves the fluidity of the molten charge. (3) Deoxidation takes place in the cupola. (4) The solidification time of the castings is shortened owing to the higher carbon content. (5) Replacing the grey by white iron

causes a considerable improvement in the mechanical properties of the product. (6) The machinability is not affected and the life of cutting tools is higher than in the case of standard cast iron. The method has proved fully successful in practice and the production costs are also favourable.

E. G.

Dilatometer Studies of Nodular Cast Iron. N. A. Ziegler, W. L. Meinhart, and J. R. Goldsmith. (American Foundrymen's Society, May 1950, Preprint No. 50-62). Nodular cast iron having a tensile strength of 70,000–90,000 lb./sq. in., yield point of 50,000–70,000 lb./sq. in., an elongation of 10–15%, and a hardness of 150–200 Brinell was made consistently from cast iron containing 3·0–3·5% C, 2·0–3·0% Si, 0·5–1·0% Mn, 0·03% S max., and 0·15% P max. Magnesium (0·25%) was used to produce the nodular graphite, and was added in the form of an 80/20 Ni–Mg alloy. About 0·5% silicon was added as an inoculating agent. Sections of the iron less than ½ in. thick were increasingly white and hard, whilst the amount and size of the graphite nodules decreased in proportion. To produce in these sections properties similar to those in the thick sections, it was necessary to graphitize the primary cementite and pearlitic carbides. From a series of dilatometric studies on large and small cross-sections of the iron, working details of the heat-treatment cycle were determined. It was found that permanent growth due to complete graphitization in sections greater than ½ in. was uniform and amounted to about 0·5% of the linear dimensions. In thinner sections it increased and at ⅛ in. it became about 1%. For complete graphitization it was necessary to hold the iron at about 950° C. for at least 2 hr. to graphitize the primary cementite, followed by about 1½ hr. at 725–625° C., or preferably at three equal stages, 725°, 700°, and 675° C., to graphitize the pearlitic carbides. The experimental heat-treatments were carried out in a vacuum of about 5 microns.

E. J. D. S.

An Introduction to the Annealing of Nodular Iron. J. E. Rehder. (American Foundrymen's Society, May, 1950, Preprint No. 50-17). Determination of the time–temperature relationships for the decomposition of primary cementite and of pearlite in a typical nodular cast iron shows that the relationships are similar in nature to those previously found for normal white irons and black-heart malleable irons, and that the kinetics and mechanisms involved must be similar. Annealing times in general are not lengthy because of the relatively high silicon contents normally present in nodular iron, but the effect of the contained magnesium is shown in slowness of removal of last traces of cementite and pearlite. Graphite precipitated during decomposition of primary cementite in nodular irons is shown to be in spheroidal form. The results of mechanical tests showed that the removal of all pearlite is not necessary for the attainment of most of the ductility found in fully annealed ferritic bars.—E. J. D. S.

Nodular Gray Iron Compared with Other Cast Ferrous Metals. G. Vennerholm, H. Bogart, and R. Melmoth. (Society of Automotive Engineers: Materials and Methods, 1950, vol. 31, Apr. pp. 51–55). *See* "Nodular Cast Iron," Journal I. and S.I., 1951, vol. 167, Feb., p. 216.

On Controlling the Shape of Graphite in Cast Iron. N. Chvorinov. (Hutnické Listy, 1950, vol. 5, June, Supplement No. 2, pp. 50–56). [In Czech]. The author investigates the factors influencing graphitization in cast iron; he believes that the dissolved oxygen and sulphur contents have a powerful effect with both ordinary and nodular cast iron.—E. G.

Investigation of Nodular Cast Iron. J. Plachý and J. Chenîček. (Hutnické Listy, 1950, vol. 5, June, Supplement No. 2, pp. 44–49). [In Czech]. The authors review briefly published information on the production of nodular cast iron and report on their own experiments in this field. Their results differ from those obtained by other research workers in some cases. Using a magnesium alloy containing a suitable percentage of copper and/or nickel they obtained a nodular structure safely without applying any protective measures. It is recommended that the alloy be added below the surface level of the melt and that inoculation should be carried out before adding it. The nickel content of the iron obtained is somewhat lower. The chemical composition, mechanical properties, and behaviour after various heat-treatments are given, and micrographs of the structure of the nodular cast iron are included.—E. G.

Where is Cast Iron Going to? P. A. Russell. (Foundry Trade Journal, 1950, vol. 88; June 29, pp. 697–702; vol. 89, July 6, pp. 7–13). After a brief historical review of the progress made in cast iron, excluding special-purpose alloy cast irons, the production and properties of nodular cast irons are examined. The differences in the production of cerium-treated and magnesium-treated iron are outlined, and the fact that feeding difficulties are less with the cerium-treated iron is noted. Transverse and tensile strength are compared with other cast irons. The regularity of the strength throughout thick sections is noted, despite the usual falling-off in strength due to mass. The damping characteristics and machinability are also described. Referring to the future, the increased markets opened up by new materials are mentioned. Problems facing the grey-iron founder include the removal or harnessing of phosphorus so that it is no longer harmful, the production of a sounder cast iron, and greater control over composition and raw materials.—J. E. W.

Production of Malleable Castings. J. H. Lansing. (Steel, 1950, vol. 126, May 22, pp. 86–97; June 5, pp. 106–118). The author gives an outline of the malleable iron industry in America. The automobile industry, agriculture, and the railways use malleable castings extensively in cases where toughness, impact resistance, ductility, corrosion resistance, castability, and machinability are of importance.—A. M. F.

Influence of Temperature on Fluidity and Surface Appearance of Steel Castings. G. A. Lillieqvist. (American Foundrymen's Society, May, 1950, Preprint No. 50-6). Experiments with the double spiral fluidity test procedure showed that temperature was one of the most important factors influencing steel fluidity and the surface finish of steel castings. Heats of intermediate manganese steels, melted in a laboratory induction furnace and poured at 2750° F., had a considerably greater fluidity than grade 'B' steels melted and poured under the same conditions, and produced a smoother surface on plate castings. There was very little difference between the results of fluidity tests on aluminium-deoxidized and on titanium-deoxidized steels of similar composition. Basic open-hearth and acid electric furnace steels of the same grade gave fluidity test results similar to those obtained from melts in the induction furnace. The fluidity in inches of spiral increased linearly from about 10 in. at 2750° F. to about 32 in. at 3000° F., with the exception of the acid steel which showed a slight deflection from this characteristic at 2900° F. Temperatures were measured by carefully placed thermocouples as the metal entered the mould. The slight differences in fluidity were attributed to differences in the sulphur content of the steels. From the study of the performance of several open-hearth furnaces it was noticed that there was a consistent drop in tapping temperature during a furnace campaign. The monthly average tapping temperatures decreased steadily from slightly above 2980° F. for the first month to about 2925° F. for the last month of the campaign—six months inclusive. An increase in furnace heating time did not offset this decrease. An explanation of these observations is not attempted. Preliminary studies on the effect of ladle preheating on metal temperature loss, using a six-ton bottom pouring ladle, showed that for this size of ladle the molten steel could lose from 200° to 300° F. for preheating times of 1½ to 4½ hr. The technique for measuring fluidity test temperatures with a platinum–rhodium thermocouple is described.—E. J. D. S.

Technique in Medium and Large Steel Castings. O. Margheriti. (Metallurgia Italiana, 1950, vol. 42, Aug.–Sept., pp. 365–367). A description is given of a method developed to eliminate the more common defects to be found in steel castings. Based on direct experience, data are given which contribute to facilitate the casting of medium and large pieces.—M. D. J. B.

Casting of Large Gear Wheels. W. H. Hopkins, J. H. West, and H. G. Goyns. (Institute of British Foundrymen: Foundry Trade Journal, 1950, vol. 88, May 11, pp. 503–508). The casting of large gear wheels in cast iron, bronze, and steel is described in detail. In each case the mould is strickled out, and in the case of steel 'cracking ribs' are incorporated in the cores to prevent hot tearing. Differences in the methods of feeding are illustrated by the fact that 6¾ tons of Meehanite 'C,' and 11½ tons of steel are required for producing the same type of wheel.—J. E. W.

Composition of the Charge for Producing High Quality Steel Castings. (Bányászati és Kohászati Lapok, 1950, vol. 5, Sept.: Öntöde, pp. 203, 208). [In Hungarian]. The composition of the various charges used for producing steel for castings in Hungary as well as extracts from the appropriate Hungarian specifications for the mechanical and electrical properties, and the chemical compositions of Hungarian steels are given.—E. G.

Steel Die Castings. (Österreichischer Maschinenmarkt und Elektrowirtschaft, 1950, vol. 5, Mar. 15, pp. 74–75). A die-casting machine for steel has recently been developed by the Hisgen Machine Tool Works in the U.S.A. In general design it is similar to orthodox horizontal die-casting machines, but it incorporates the following special features: The cylinder is preheated by oxy-acetylene burners; the plunger is shaped to fit the spout so that the whole of the molten steel is expelled; all parts of the machine in contact with molten steel are protected by a coating of special steel (*e.g.*, high-alloy chromium steel) or tungsten carbide; and the chromium–nickel steel dies are protected by a silicone-base lubricant. The steel is melted at the rate of about 250 kg./hr. in a high-frequency furnace supplied by a 175-kW. generator. The process is still in the development stage and is at present limited by the melting point of the steel to be cast, but castings weighing up to 10 kg. have been made.—H. R. M.

Influence of Dry Sand Conductivity on Rate of Freezing of Steel Slabs. V. Paschkis. (American Foundrymen's Society, May, 1950, Preprint No. 50-7). The sands used in the investigations reported were those designated 20–30, 50–70, and No. 7 by C. F. Lucks and co-workers (*see* Journ. I. and S.I., 1947, vol. 157, Sept., p. 144). From experimental data a curve was plotted for each sand to show how the time taken for the steel temperature to fall from the pouring to the solidus temperature and from the pouring to the liquidus temperature, varied with the distance from the mould–metal interface. Sand with a thermal conductivity given as 0·4 B.Th.U./ft./hr./°F. gave the shortest steel-cooling times. The time for the centre of the casting to reach the liquidus was also plotted against the conductivity of each sand. This showed that with very low sand conductivity the solidification time increases and that as the sand conductivity increases its influence becomes less effective. It appears to have little significance above about 1·0 B.Th.U./ft./hr./°F.—E. J. D. S.

Some Factors Affecting the Characteristics of Foundry Sands. M. Ongaro. (Metallurgia Italiana, 1950, vol. 42, Oct., pp. 352–356). A study is made of the influence of humidity and organic agglomerates on the permeability of foundry sands. The influence of humidity on the cohesion of sands is also examined, as well as that of quartz powder and iron oxide. The temperature gradient in relation to grain size and cohesion is also studied.—M. D. J. B.

Contribution to the Problem of Preparation of Moulding Sands. L. Petržela. (Hutnické Listy, 1950, vol. 5, Sept., pp. 353–359; Oct., pp. 406–412). [In Czech]. The author reviews the deficiencies of natural sands and the objects of sand preparation and reports the results of tests on processes of preparing sand. The influence of drying on the properties of green glauconite sand was determined after drying for 4 hr. at the temperatures 90°, 110°, 300°, and 400° C. samples which had been prepared for periods of 5, 10, and 20 min. in a Simpson mixer. Compression strength and permeability decreased with increasing drying temperatures, but the decrease was reduced by a longer mixing time; the moisture content corresponding to optimum compression strength remained practically unchanged. The compression strength of specimens mixed in a rib-type mixer, changed gradually from 900 g./sq. cm. to up to 1250 g./sq. cm. after 1 hr. mixing; this reduced the optimium moisture content from about 6% to 4% after a mixing time of 60 min. In the case of sands mixed with bentonite maximum compression strength was obtained after mixing for 10 to 20 min.—E. G.

The Use of Sulphite Lye in Casting. (Hutnik, 1950, vol. 17, July–Aug., Biuletyn Informacyjny Glownego Instytutu Metalurgii i Odlewnictwa, p. 21). [In Polish]. The use of sulphite lye as a binder for moulding materials is described. V. G.

New Sand Handling Installation at Vandergrift. A. J. Coulson. (United Effort, 1950, July–Aug., pp. 12–14). The $300,000 installation is unique in that outside storage is provided for 500 tons of screened backing sand, all the plant, with the exception of the shake-out machine, being built below the floor or outside the foundry. The plant includes: (1) A 100-ton shake-out machine, 12 ft. × 16 ft. in four units, 6 ft. × 8 ft. each, working singly or in unison; (2) a 3000-lb. capacity muller-type sand mixer; (3) a 2000-lb./min. hydraulically controlled 'Speedslinger' with a 22-ft. radius.—P. C. P.

Determination of the Moisture Content of Moulding Materials. L. Jeníček. (Hutnické Listy, 1950, vol. 5, June, Supplement No. 2, pp. 12–20). [In Czech]. The direct, differential, and indirect methods of moisture determination are briefly described. The most suitable are the differential weighing method and the indirect method based on a chemical reaction with carbide resulting in gas generation. The former is suitable only for laboratory work; the time required can be shortened by speeding up the drying of the specimen and by improving the weighing. The author reports on a design of balance for obtaining the difference in weight before and after drying. The carbide method is suitable for foundry use. To obtain sufficiently accurate results intimate contact between the carbide and the moist material is essential. The author describes a 'finger-like' rubber container and a mortar with an elastic seal on top in which intimate contact between the carbide and the sample is obtained by kneading. The formula for calculating the humidity contains several terms and by keeping two of them constant the calculation is sufficiently simple for graphs to be applied. Test apparatus designed according to the principles discussed is described; with this the moisture in a specimen weighing several grams can be determined in 2–3 min. with an accuracy of ± 2%. The prototype is fitted in a wooden box 14·5 × 16·5 × 43 cm. weighing only a few kilogrammes.—E. G.

Technical Evaluation of Clay Binders and Sands Used for Casting in Green Sands. L. Petržela. (Hutnické Listy, 1950, vol. 5, June, Supplement No. 2, pp. 21–43). [In Czech]. The author reports on experiments to determine the interrelation between compression strength, moisture content, permeability, and clay-binder content of moulding materials. Twenty different kaolinitic and montmorillonitic specimens were tested to establish the relative importance and the mutual influence of the individual factors and particularly on the interaction of the fine fractions of the sand with the binder. The data and properties of the sands are given and the treatments of the sand and clay binder are described. The quality of the clay binder and the properties of active admixtures present in the sand are the main factors influencing the strength and permeability of the mould material. The ideal case of a pure sand-binder mixture rarely occurs in practice. The importance of considering the influence of decomposition products arising from the casting process is emphasized. The volume of the gel produced has only a slight influence on the permeability of the mould material, but the influence of its distribution on the individual grains is considerable. Formulæ are given for calculating the quantity of clay binder required to obtain a certain compressive strength, and the compressive strength for a given moisture content. Finally, the author summarizes the errors which may occur in determining certain properties. The data and results of the experiments are presented in 66 graphs and 10 tables.—E. G.

Notes on the Prospecting of Moulding Material Deposits in Slovakia. J. Wurm. (Hutnické Listy, 1950, vol. 5, June, Supplement No. 2, pp. 5–7). [In Czech]. Slovakian quartz sand deposits are mostly found in younger tertiary formations. These sands contain high percentages of impurities, particularly clay admixtures and their utilization is adversely affected by the presence of gravel beds.—E. G.

Foundry Sands in Slovakia. V. Uxa. (Hutnické Listy, 1950, vol. 5, June, Supplement No. 2, pp. 8–11). [In Czech]. The prospects of supplying home-produced sands to the foundries in Slovakia are discussed. At present about 70% of the total foundry-sand requirements are bought from Bohemia and Moravia.—E. G.

Production Patterns and the Matchplate. R. F. Dalton. (American Foundrymen's Society, May, 1950, Preprint No. 50-66). The development of production pattern equipment is reviewed, and advantages and disadvantages of various types of pattern equipment are described. The use of matchplates in the foundry is determined by (1) production requirements and costs, (2) size of mould to be handled, and (3) reduced degree of skill required in moulding. Because of the

high degree of accuracy obtained in manufacture of pressure-cast aluminium matchplates, a better knowledge of some of the properties of metal casting plaster as a moulding material may be helpful in the production of other non-ferrous castings.
E. J. D. S.

Patternmaking for General Engineering Castings. H. S. W. Brittain. (Foundry Trade Journal, 1950, vol. 88, Apr. 13, pp. 389–397; Apr. 20, pp. 427–431). The equipment required in a pattern shop, making both wood and metal patterns is enumerated. The properties of the pattern-making woods, Canadian pine, white pine, mahogany, and teak, together with other pattern-making materials, *e.g.*, plaster, stone, cast iron, brass, gunmetal, aluminium, aluminium alloys, and plastic are described and their relative merits assessed. Thereafter, numerous individual patterns are described as examples which relate to pattern practice for serving various moulding techniques—in green sand, dry sand, and loam.—J. E. W.

Flow of Metal through Runner Systems. D. F. B. Tedds. (Foundry Trade Journal, 1950, vol. 88, Apr. 27, pp. 443–452). Molten metal flowing through normal foundry running systems always has a turbulent motion. Using glass-fronted plaster of Paris moulds and mercury, the turbulence produced by various runner systems has been photographed. The author's conclusions are: (1) The effect of turbulence is particularly intense both at the base of the sprue, where the velocity is at a maximum and where the energy of the flowing metal is liberated by its impact at the base, which causes rupture of the vertical jet and its projection in all directions, and also at cross gates where the velocity remaining is favourable to the formation of jets which impinge against vertical walls thus promoting oxide formation. (2) Runner systems should be designed which reduce the velocity of the metal stream, avoid sharp angles and abrupt changes in direction, and do not impinge directly on mould walls. (3) The incorporation of a well or turbulence cushion at the base of the sprue will reduce the velocity and the turbulence.—J. E. W.

Moulding on the Herman Type Moulding Machines. J. Širokich. (Hutnické Listy, 1950, vol. 5, June, Supplement No. 2, pp. 62–66). [In Czech]. Brief descriptions are given of the various types of moulding machine with vibrator and tilting table produced by Zimmermann, and Badische Maschinenfabrik (Germany); Mascheroni, and Rabuffetti (Italy); Krasnaya Presnya Works (U.S.S.R.); and the American firms Osborn, Davenport, and particularly Herman. The results of comparative time studies carried out in Czech foundries on some of these are reported. The paper includes a drawing and data of the Russian moulding machines which are similar to the Herman Nos. 750 and 1500.—E. G.

Moulding Machine of Novel Design. (Foundry Trade Journal, 1950, vol. 88, May 25, pp. 562–563). This machine, developed by the British Moulding Machine Company, has been designed for the rapid production, between two and three half moulds per minute, of stove plate and other relatively flat castings. The machine, which is essentially of a 'down-sand-frame type,' also removes the mould and turns it over. Other features incorporated are a vibrator which automatically operates during the squeeze stroke, and a variable electrical heat control of the pattern.—J. E. W.

Production of Cores by Compressed Air. Bradač. (Hutnické Listy, 1950, vol. 5, Sept., pp. 392–393). [In Czech]. The author describes the practice applied for the production of cores in the Agrostroj foundries at Jičín, Czechoslovakia. The composition of the core sands used for various purposes is given. The cost of labour for producing cores has been cut by 70% by applying core-blowing machines and delivery time was reduced from 6 weeks to 16 workings days. About 40% of all cores for malleable castings are produced by core-blowing machines in this foundry. Cast iron proved to be most suitable for core boxes, since there is a danger of explosion with wooden ones.—E. G.

Loam and Dry-Sand Moulding in the Jobbing Foundry. D. Redfern. (Foundry Trade Journal, 1950, vol. 89, July 13, pp. 37–43). The methods used in moulding a cylinder block casting and a water-box casting are described in detail. The cylinder block was moulded in a dry sand; the water box in loam, using strickle boards and a skeleton pattern. Of interest are remarkably detailed figures given for the manufacture of three castings made in 1907.—J. E. W.

Work in a Jobbing Foundry. J. F. Dowell and H. London. (Foundry Trade Journal, 1950, vol. 88, Mar. 30, pp. 341–347; Apr. 6, pp. 365–369). Problems experienced in a jobbing foundry are explained and the methods employed in making 'out of the ordinary castings' are detailed. This often entails working with a minimum of pattern equipment in the production of green-sand, dry-sand, and loam moulds. Typical castings include cylinder ends, tank plates, green sand-moulded vats, pipe moulds (with shell or loam patterns), dry-sand castings for high pressure, vacuum, or high-duty purposes, loam moulding of paper-drying cylinders, and plate-pressing dies. The operation of the cupola is outlined, and the properties of the sands used are also given.—J. E. W.

Cold Formed Flexible "Precision" Patterns and Core Boxes. R. B. Wagner and J. E. Wiss. (American Foundrymen's Society, May 1950, Preprint No. 50-65). Flexible precision patterns and core boxes are considered to be ideal equipment for the forming of metal casting plaster, investment plaster, complex wax, or other thermosetting positives. A step-by-step description of one method of making flexible cope and drag multiple patterns in a new type of synthetic rubber, starting with a single loose piece split pattern, is described. Illustrations are given to show the steps involved. Previously used materials and applications of the new method are discussed.—E. J. D. S.

The Casting of Metals in Permanent Molds. G. G. M. Carr-Harris. (Canadian Metals, 1950, vol. 13, Aug., pp. 26, 28, 29, 44–46). The technique of gravity die-casting, known in America as 'permanent mold casting,' is reviewed, with frequent reference for details to a bibliography of 49 English and American publications. Definitions of variations of the process are given, followed by notes on procedure, design, and treatment of moulds, control requirements, and mould castings.—E. C.

The Problems of Centrifugal Casting. V. Koutecký. (Hutnické Listy, 1950, vol. 5, June, Supplement No. 2, pp. 57–61). [In Czech]. The author believes that the mechanical properties of carbon steels are not improved by centrifugal casting and that this method has a favourable influence only on the quality of alloyed steels.—E. C.

New Specialized Heavy-Casting Foundry: Slag-Ladle and Ingot-Mould Production at Distington Engineering Company Limited. L. H. Williams. (Iron and Coal Trades Review, 1950, vol. 160, May 26, pp. 1171–1180). **Steelworks Castings.** L. H. Williams. (Foundry Trade Journal, 1950, vol. 89, Aug. 3, pp. 125–130). The author describes the general layout and equipment of the foundry of the Distington Engineering Co., Ltd., dealing briefly with the pattern shop, melting facilities, crane power, sand-mixing plant, moulding machines, stripping and machine shops, and drying stoves. The foundry, originally designed as an electric steelmaking plant, specializes in the production of slag ladles and ingot moulds, the methods employed being described in some detail.—G. F.

Modernising a Gear Foundry. (Foundry Trade Journal, 1950, vol. 89, July 6, pp. 15–18). The century-old foundry and gear manufacturing works of David Brown-Jackson, Ltd., Salford, was awkward in shape, and had the additional disadvantage of being bounded by a canal; it was also in close proximity to two roads and a railway. Reorganization work concentrated therefore on full utilization of moulding-floor space, and was achieved by rationalized mechanical handling of materials and products. A telpher system has been installed to convey charges straight to the melting platform, and an overhead travelling crane ensures speedy delivery of moulding boxes to and from the foundry. Sand is unloaded *via* a chute into bins, from which it is delivered into the mills, which have been fitted with water-sealed lids against dust pollution of surrounding areas. A fully mechanized gas-producer plant serves two open-hearth furnaces, mould-drying stoves, and an annealing stove. Particulars are given of the means provided for illuminating the works and the foundry.
J. E. W.

Modernising an Iron Foundry. L. W. Bolton and W. D. Ford. (Institute of British Foundrymen: Foundry Trade Journal, 1950, vol. 88, May 25, pp. 551–558). The mechanization of the Wellingborough Foundry of Morris Motors, Ltd., introduced many problems. Thirty-five components for an agricultural tractor are made, varying in weight from a few ounces to 500 lb. The plant is designed to operate in three sections, involving light, medium, and heavy castings. The light section is provided with straight draw, jolt-squeeze machines, the sand being fed from overhead by a continuous mill which also supplies the sand-slingers in the heavy section. The medium-size castings are moulded by jolt-squeeze

turnover machines. A synthetic sand mixture is used, losses being replaced by burnt core sand, the sand properties being kept within well-defined limits. In order to maintain a standard drying time cycle, large cores are bonded with a phenolic resin–starch binder. A clean water spray before stoving is claimed to give such cores an improved surface hardness. To provide the high-duty iron, metal of a base composition is melted and any adjustments required are made in the ladle. Methods of providing and controlling such irons are critically examined. The attention paid to dust control is detailed; side suction hoods have been found satisfactory at the 'shake-outs' in each section. The heating, lighting, and cleaning problems of the foundry are described. To reduce the amount of fettling necessary, the 'Connor' runner is employed where possible. A good facing sand and sprayed mould coatings also help in this direction.—J. E. W.

The Holman Foundry. (Foundry Trade Journal, 1950, vol. 88, May 4, pp. 469–473). This Cornish foundry originally made castings for tin-mining machinery, but present products include air compressors, rock-drill sharpening machines, small pneumatic hoists, scraper-haulage gear, etc. Castings vary in weight from a few pounds up to two tons and besides iron, non-ferrous and steel castings are produced. The steel is melted in a Stein and Atkinson rotary furnace, and the Meehanite iron by well-controlled cupolas. The melting and the sand sections are controlled directly from the laboratory, the sand being obtained from the firm's own quarries. Despite the high standard of casting inspection, the foundry scrap figure is less than 10%.—J. E. W.

Indian Foundry Industry. V. M. McGowan. (Foundry Trade Journal, 1950, vol. 88, Apr. 20, pp. 417–423). The general foundry of the Indian Iron and Steel Company, Bengal, consists of two bays, each 50 ft. × 720 ft., and has a monthly production of 1500 tons of iron castings. Precautions to overcome climatic conditions involve east to west alignment, north lighting and open-ended buildings. The four classes of work performed are: (1) Green sand; (2) light and heavy dry sand; (3) loam; and (4) core-sand moulding. Melting is conducted by three cupolas, two of which are 49-in. dia. balanced-blast cupolas. Owing to the high ash content of the local coke, and the great variations in humidity experienced, melting difficulties are increased, though satisfactory spout temperatures of up to 1350° C. are obtained. A careful control is exercised upon the local sands used. The somewhat traditional moulding methods used in making pipes, pile screws, and chilled rolls are described.—J. E. W.

Modern Australian Grey-Iron Foundry. V. A. Saveneh. (Foundry Trade Journal, 1950, vol. 88, Mar. 30, pp. 335–340, 348). The new foundry of General Motors Holden, Ltd., which provides grey iron automobile castings, covers a floor area of 51,000 sq. ft. and the layout has been designed with special regard for future expansion. Points of interest in this fully mechanized plant are the provision of 'constant air-weight' blowers for the cupolas, a duplex melting process, and the extensive use of air-hoists for lifting small weights. J. E. W.

Burn's Court Foundry Reconstruction. J. Blakiston. (Foundry Trade Journal, 1950, vol. 88, Mar. 23, pp. 307–316). This reconstructed iron and steel foundry at Howrah, Calcutta, consists of five main sections, *viz:* the steel foundry, the general iron foundry, the main mechanized section, and the heavy and light mechanized sections. These are all under one roof and are intercommunicating. Partial mechanization has been developed, the degree of which varies according to local conditions. The sand-handling plant is modern, except for the provision of shovel hoppers, and it appears that the steel and iron foundries share the same sand plant. The cupola plant is also fully mechanized. Owing to the climatic conditions, the building is arranged to induce an air current through the foundry. In addition, arrangements are made to cool the hot sand, and also remove the hot castings from the vicinity of the workers.—J. E. W.

High Standards in Mass Production Foundry. G. L. White. (Canadian Metals, 1950, vol. 13, Aug., pp. 16–19, 47). Organization and methods of the 'M' Foundry, Massey Harris Co., Ltd., Brantford, Ont., are described, and outlines given of production planning and control. Designed to produce 35 tons of grey iron castings per 8-hr. day, production has reached 140 tons in two 8-hr. shifts. Mechanized equipment, overhead rails, and belt conveyors are used extensively. Four mechanically served cupolas (7 ton/hr. each) operate alternately in pairs. Lines of mould production comprise two for jolt-squeeze moulds (castings up to 15 lb.), and two for jolt roll-over moulds (castings up to 150 and 750 lb.). Metals and sand are closely controlled. Daily production of cores is 150,000, mainly by core blowers, close scheduling avoiding large core storage. Cleaning follows normal practices.—E. C.

Feature of Iron and Steel Foundry is Flexibility. (Canadian Metals, 1950, vol. 13, July, pp. 20–23). A modern steel foundry at the Dominion Engineering Works, Lachine, Que., is described, with emphasis on the rational floor layout, mechanization, and economical handling of materials. The plant includes an 1800-kVA. Lectromelt furnace, bottom-pour ladles, a combined transfer and weighing car, mechanized sand-handling equipment, jolt roll-over machines and jolt squeezers for machine-made moulds, and quickly interchangeable plywood-mounted patterns. Man-handling is practically eliminated.—E. C.

Productivity Standards in Foundries. J. Přibyl. (Hutnické Listy, 1950, vol. 5, Sept., pp. 367–373). [In Czech]. Means of increasing the productivity of labour in foundries are discussed and the author presents a method of specifying the productivity of individual shops and workers which is suitable for evaluating the output for all types of casting. The percentage of casting rejects, degree of utilization of metal, consumption of fresh sand, and adherence to delivery dates are also taken into consideration. The procedure for specifying output is illustrated by a practical example in which the output standards are drafted for a foundry worker per week and his productivity is evaluated taking all the above factors into consideration. Formulæ to be used for the calculation of such output specifications are given.—E. C.

Heavy Castings and Forgings Made in Czechoslovakia. (Hutnické Listy, 1949, vol. 4, May, Supplement). A few examples of the heaviest castings and forgings produced in Czechoslovakia are shown in seven illustrations.—E. C.

Mechanised Production of Light Castings. A. G. Thomson. (Foundry Trade Journal, 1950, vol. 88, Mar. 16, pp. 277–279). The mechanization of the Wilmer Lea Foundries, Ltd., Bow Bridge Iron Foundry, London, is described. The foundry produces builders' castings, weighing 1 to 15 lb., at the rate of 8 tons/day.—J. E. W.

Roller Conveyors and Fittings. (Foundry Trade Journal, 1950, vol. 88, Mar. 16, pp. 275–276, 280). Methods of overcoming the limitations associated with roller conveyors are given. These include the use of aluminium mould carriers, roll-over points, and air-operated manipulators.—J. E. W.

Ruston and Hornsby's New Manufacturing Facilities.—The Beevor Foundry. (Foundry Trade Journal, 1950, vol. 88, Mar. 16, pp. 285–295). *See* Journ. I. and S.I., 1950, vol. 165, May, p. 112.

Casting for Contractors' Plant. A. G. Thomson. (Foundry Trade Journal, 1950, vol. 89, Aug. 10, pp. 143–157). The foundry of Winget, Ltd., Rochester, employs 150 men to produce a weekly output of 70–80 tons of Meehanite castings ranging in weight from a few ounces up to 35 cwt. Castings made, predominantly in GE or GA Meehanite, include drum bases and road wheels for concrete mixers, pressure-resisting castings; refrigerator castings; and lathe beds. The system of quality control employed enables limits of 0·1% for carbon and 0·15% for silicon to be maintained and control is also exerted over all the other constituents. The Dean and Starke method, employing toluene, is adopted for measuring moisture in oil-bonded core sands.—J. E. W.

Materials Handling in the Foundry. F. O. Lemmon. (American Institute of Mining and Metallurgical Engineers, Electric Furnace Steel Conference Proceedings, Dec., 1949, vol. 7, pp. 33–45: Journal of Metals, 1949, vol. 1, Nov., pp. 36–37). At the Ohio Steel Foundry railway trucks of sand are unloaded through the bottom into a hopper from which the sand is moved by an underground belt conveyor and elevator system. Scrap is moved from truck to storage bin by crane and magnet at the rate of 50 tons in 4 hr. Materials are moved from storage sheds to production departments by tractors pulling flat-bottomed trailers, skids, or side-tipping trailers. Fork-lift trucks are used for smaller loads. One-man monorail ladles bring molten metal to the casting department.

In the discussion, R. C. Howell gave an outline of a steel foundry having ideal material-handling equipment.—R. A. R

A Thermodynamic Study on Pinhole Formation in Steel Castings. R. E. Savage and H. F. Taylor. (American Foundrymen's Society, May, 1950, Preprint No. 50-28). Pinhole formation in steel is considered to be a kinetic problem rather than one of thermodynamics, but dependable data are lacking. The authors consider that, from a thermodynamic aspect, the indications are that: (1) Hydrogen dissolved in steel does not create enough gas pressure to form cavities; (2) the hydrogen–oxygen reaction with formation of water vapour does not occur; (3) the most probable cause of pinholes is carbon monoxide gas; and (4) carbon monoxide is formed by reaction of the carbon in the steel with iron oxide at the mould-metal interface. Aluminium in solution in the steel prevents pinhole formation.—E. J. D. S.

Effect of Liquid Metal Properties on the Casting Fluidity of Alloys. V. Kondic. (Institute of British Foundrymen: Foundry Trade Journal, 1950, vol. 88, June 29; pp. 691–695). The problem of fluidity resolves itself into two main questions: (*a*) To what extent and in what manner is the casting fluidity related to the inherent properties of liquids, and (*b*) how are such relationships that exist under (*a*) modified in impure metallic liquids? Casting fluidity is controlled by mould and metal properties as well as by casting factors, but this paper only discusses the metal properties. Inherent liquid properties such as heat content, viscosity, surface tension, and constitution are defined, and their relative effects critically examined. Various non-inherent liquid properties, including gas content, surface oxide films, and unintentional impurities also have a great effect on casting fluidity and the probable effect of each is examined. Finally, in discussing problems in this field still remaining, the author stresses the need for the accumulation of more data to allow a better understanding of the phenomena involved and also, for the foundry industry, to simplify the technique while keeping the sensitivity. Finding the right type of fluidity test for each metal, and the right conditions for its use, are the remaining important problems.—J. E. W.

Tung Oil Seals Porous Castings. G. J. Dull. (Iron Age, 1950, vol. 165, June 29, pp. 76–78). A variety of casting is being impregnated with tung oil to seal leaks due to micro-shrinkage and micro-gas-porosity. Castings so treated can be used at temperatures of 1000° F. and pressures up to 3000 lb./sq. in. After impregnation the castings are baked at 350° F. and it is this operation which causes polymerization of the tung oil which then becomes in effect an integral part of the casting.—A. M. F.

The Formation of Cavities (Blowholes) in Castings. F. Roll. (Neue Giesserei, 1950, vol. 37, Feb. 9, pp. 45–52). The most troublesome cause of shrinkage cavities in castings, the so-called 'edge effect' due to sharp edges in the mould or core, is described and discussed. Cavities are caused by gas evolution, often under pressure, due to local overheating of the material of the mould. They are sometimes accompanied by true shrinkage cavities. Examples are quoted from cast iron, malleable iron, and steel-casting practice.—O. H. G.

Foundry Cleaning Room Abrasive Operations. B. H. Work. (American Foundrymen's Society, May, 1950, Preprint No. 50-61). The purposes controlling the choice of type of equipment for rough-grinding castings in the foundry, power and abrasive wheel requirements, the deployment of the operatives, and operational technique are discussed.—E. J. D. S.

Classification of Castings According to Weight and Complexity. A. Plěsinger. (Hutnické Listy, 1950, vol. 5, May, p. 218). [In Czech]. The author referred to Soviet practice and proposed that castings should be classified in Czechoslovakia into six weight groups, three standard complexity groups, and a fourth group for particularly complex castings. In the U.S.S.R. castings are classified into nine weight groups and Joffe has introduced there additional classification of three groups according to complexity. It was suggested that the Czech Standard Specifications concerning machining allowances on castings should conform as much as possible to the corresponding Russian GOST specifications, because the U.S.S.R. is at present the largest customer of the Czech foundries.—E. G.

Education for Foundry Personnel. W. F. Seivewright. (Foundry Trade Journal, 1950, vol. 88, Apr. 6, pp. 361–364). The Meehanite Training Scheme imparts technical knowledge in foundry work, and simultaneously endeavours to inculcate character training, a broadening of outlook, and the team spirit. The training of instructors is very important.

HEATING FURNACES AND SOAKING PITS

Soaking Pits and Reheating Furnaces. (Metallurgia, 1950, vol. 42, July, pp. 82–85). Details are given of some of the most recent soaking pits and reheating furnaces in Great Britain. These include examples of the most modern developments in one-way and reversing-type furnaces, some incorporating regenerating and recuperating systems. The fuels used in these furnaces vary from blast-furnace gas to clean anthracite gas firing. The largest slab reheating furnace in Europe and several examples of continuous bogie furnaces are described.—J. J.

Production of Semifinished Steel. K. L. Fetters and H. H. Hottel. (Steel 1950, vol 126, Apr. 10, pp. 102–112; Apr. 17, pp. 86–93; Apr. 24, pp. 86–90). The authors discuss the heating of steel ingots and the various types of soaking pit in use, fuels utilized, and the methods of heating employed. The importance of correctly heating an ingot is stressed. The operation of two- and three-high blooming mills, slabbing mills, and three-high and continuous billet mills is described.

A. M. F.

Controlled Cooling of Steel Blooms in Car-Bottom Furnaces. C. J. F. Burch. (Iron and Steel Engineer, 1950, vol. 27, June, pp. 86–94). This paper describes proposed bloom-handling facilities for the controlled cooling of steel blooms in car-bottom furnaces. The advantages of controlled cooling over slow cooling and the factors governing the selection of the car-bottom furnace are discussed. The car-bottom furnace, the handling facility requirements, the car-loading procedure, and the capacity and size of furnace are also considered.

M. D. J. B.

Controlled Heating for Rolling. W. H. Dailey, jun. (Iron and Steel Engineer, 1950, vol. 27, May, pp. 108–110). The general problem of heat control for low- and high-speed heating is briefly discussed as well as the requirements for control devices for both slow and fast heating. The equipment and the operating and maintenance organizations are considered. Controlled heating for rolling temperature, thermocouples and radiation receivers, automatic devices for fuel–air mixture and automatic pressure control are also considered.—M. D. J. B.

Absorption of Sulphur by Solid Steel from Flue Gases. L. Sass. (Bányászati és Kohászati Lapok, 1950, vol. 5, May, pp. 329–330). [In Hungarian]. Results of experiments on the absorption of sulphur by solid steel during heating by flue gases are reported. Four specimens from each of four steels (a carbon steel, a chromium–nickel steel, a 13% chromium steel, and an 18/4/1 high-speed steel) were machined and placed at the rear of a smith's fire to avoid direct contact with the flame; the temperature was 1050–1150° C. The SO_2 content of the gases varied between 2·01 and 13·8 g./cu. m. The specimens were withdrawn successively after 1, 2, 4, and 8 hr., cooled in air, and the scale removed. Five 0·5-mm. layers were then machined off and the sulphur contents of each determined. The author concludes that the sulphur absorbed is in most cases harmless unless the steel is already affected by intercrystalline corrosion.—E. G.

Automatic Gas-Fired Forge Furnace Meets Need for Versatility. J. R. Ross. (Industrial Heating, 1950, vol. 17, Mar., pp. 426–431). A description is given of the installation of a new gas-fired billet-heating furnace at the Chevrolet Forge Division of General Motors Corporation, Detroit. The furnace is of the walking-beam type, and can be used for heating bars up to 1½ in. in squares, rounds, and flats, in lengths from 12 to 30 in., to a temperature of 2250° F. The efficiency of the furnace is reflected by the gas consumption of 3000 cu. ft. of 1000 B.Th.U. natural gas per ton of steel heated.—J. A. L.

Principles of the Induction Heating of Steel and Its Application in Forges. K. Podgorecki. (Hutnik, 1950, vol. 17, July–Aug., pp. 217–225). [In Polish]. The fundamental interdependence of the parameters of induction heating such as frequency, current, and time of heating are discussed. The description of inductors and the influence of their shape on heating properties, types of generator of increased frequency, and heating installations in a modern forge are given.—V. G.

Gas Economies in Industry. R. D. Keillor. (Gas Heat in Industry, 1950, vol. 14, Oct., pp. 4–6). Weaknesses in established heating techniques fall under two main headings, (*a*) speed of heat transmission, and (*b*) waste of fuel while work is being moved progressively from one heating station to another. The first can be dealt with by using 'snappy'

short flames close to the work or a combination of short flames and radiant surfaces. The second can be remedied by using burners controlled by one or more economy valves geared to, or cam-actuated from, the main drive so that gas consumption is reduced to a minimum while the work is moving between stations. Another remedy is the fitting of a rotary gas distributor so that the burners are carried round with the work. R. A. R.

Molten Glass a Heating Innovation. D. L. Campbell. (Iron Age, 1950, vol. 165, June 15, pp. 95–98). This paper describes a new form of electric heating for metallurgical furnaces. Heat is radiated from a bath of molten glass through which current is passed from submerged electrodes. The chief advantages are that high temperatures up to 2700° F. can be readily obtained, and there is a tranquil atmosphere in the furnace. Examples are given of the application to various types of furnace.—A. M. F.

HEAT-TREATMENT AND HEAT-TREATMENT FURNACES

The Influence of Chemical Composition on the Strength of Structural Steels. R. Zoia. (Metallurgia Italiana, 1950, vol. 42, Oct., pp. 357–361). A formula for the heat-treatment of steels at 600° C. is referred to and an extension of this formula to a range of temperatures is proposed. The formula relates ultimate strength to the chemical composition of structural steels and shows that every element brings in a factor affecting ultimate strength, but that temperature only affects the carbon parameter. With equal quantities of carbon and varying temperatures, the heat-treatment curves undergo only parallel displacements. Except for secondary effects, which are briefly described, the variation in strength of steels, due to heat-treatment temperatures, is determined by the coagulation of the cementite.—M. D. J. B.

The Hardening Operation in the Practical Heat Treatment of Steel. Q. C. McMillan. (Journal of the West of Scotland Iron and Steel Institute, 1948–49, vol. 56, pp. 98–125). The layout of a mechanized heat-treatment plant is given with details of some of the equipment installed. Theoretical aspects of hardening are considered in some detail, with reference to time-quenching, **S** curves, austempering, martempering, marquenching, the Jominy end-quench hardenability test, and the effect of grain size on cracking. The effect of a number of practical items, such as soaking time in the furnace, on mechanical properties is discussed.—P. C. P.

Recent Heat Treatment Furnace Installations. (Metallurgia, 1950, vol. 41, Apr., pp. 332–340). A description is given of some recently constructed heat-treatment furnaces. These include hardening, annealing, normalizing, and brazing furnaces, with or without a controlled atmosphere, and the roller hearth, rotary hearth, mesh belt continuous, and pusher intermittent types of furnace as well as batch furnaces are also described. The heating of the furnaces described is by either gas or electricity. The trend is noted towards the development of furnaces specifically adapted to one purpose as opposed to general use.—J. J.

Heat Treatment of Steel Forgings for Gas Turbines. H. W. Kirkby. (Metallurgia, 1950, vol. 41, Apr., pp. 317–320). A description is given of the heat-treatments necessary to obtain the best results from both ferritic and austenitic creep-resisting alloy steels for gas-turbine installations destined to be used on land or at sea. Treatments are described in which forgings are raised to 1200° C. The considerable sizes of some of the treated items make handling at these high temperatures difficult. In most of the ferritic steels and the lower-grade austenitic steels no great difficulties are presented, but the high solution temperatures of the most complex austenitic steels present difficulties, which can, however, generally be surmounted using normal equipment but with greater precautions.—J. J.

Getting More Out of Heat Treating. H. E. Boyer. (Steel Processing, 1950, vol. 36, Apr., pp. 195–198; May, pp. 251–253, 256). The first part of this article reviews, in an elementary manner, the basic principles involved in the heat-treatment of steel. The allotropy of iron, the rôle of carbon, the **S**-curve, and transformation products for a plain carbon steel are described. The second part discusses, with the aid of microphotographs, the properties of two different steels after annealing, hardening, and tempering. These are a plain 1% carbon steel, and a 2·25% carbon, 12·5% chromium steel with smaller amounts of molybdenum and vanadium.—P. M. C.

Heat Treatment of High Pressure Cylinders. S. M. Stoler. (Steel Processing, 1950, vol. 36, June, pp. 302, 303, 317). The article describes the new continuous plant, installed at the Taylor-Wharton Iron and Steel Co. (U.S.A.) for the heat-treatment of gas storage cylinders. The heating furnace is of unusual design, conventional oil burners firing tangentially into the tubular-shaped chamber. The gas cylinders move along the central axis of this chamber and are thus surrounded by a rapidly rotating band of hot gases.—P. M. C.

Quality Heat Treating Builds Small Shop. J. C. McComb. (Steel Processing, 1950, vol. 36, June, pp. 297–301). The author describes the equipment of a small precision heat-treatment shop. This includes ten furnaces, all with atmosphere and temperature control, adequate oil, water, brine, and salt-quenching facilities plus units for testing, straightening, and cleaning. There are nine illustrations.—P. M. C.

Selecting Steels and Heat Treatments for Automotive Gears. V. E. Hense, H. H. Miller, and R. B. Schenck. (Materials and Methods, 1950, vol. 31, June, pp. 57–60). This article discusses the economics of rear axle, transmission, and flywheel ring gear production. Economy in the selection of the type of steel is discussed with reference to availability, cost, machinability, heat-treating qualities, performance properties, and uniformity. The selection of economic heat-treatment processes is discussed under the headings: Annealing, hardening, and carburizing.—P. M. C.

Special Gas Furnaces Cut Fuel Costs, Improve Dry Cyaniding. (Steel, 1950, vol. 126, Apr. 10, pp. 85–86). Three new gas-fired radiant-tube furnaces at the Commercial Steel Treating Co., Detroit, are reported to be capable of saving 40% in fuel costs. They are natural-gas-fired with special burners and a refractory mass of a special shape surrounds the radiant tubes to store up heat during the heating cycle. A circulating fan ensures that fresh gas continuously surrounds each part in the furnace. Oil is circulated in the quench tank by a propeller to ensure uniform quenching.—A. M. F.

Heat Treatment and Properties of Iron and Steel. S. J. Rosenberg and T. G. Digges. (United States Department of Commerce, National Bureau of Standards, Aug., 1950, Circular 495). This 33-page circular presents in simplified form the basic theoretical and practical principles involved in the heat-treatment of steel and cast iron.—R. A. R.

Modern Heat Treatment Furnaces, with Special Reference to Town's Gas Firing. L. G. A. Leonard. (Manchester Association of Engineers, 1950, Advance Copy). An illustrated revue is presented of many types of modern annealing and heat-treatment furnaces.—R. A. R.

Electric Furnaces for Melting and Heat-Treatment. F. Knoops. (Archiv für Metallkunde, 1949, vol. 3, July, pp. 243–247). Different types of small electric furnace for melting non-ferrous metals, as well as salt-bath and other heat-treatment furnaces for steel are described with data on capacity, temperature, and current consumption.—R. A. R.

Modern Heat Treatment. Part I—Salt Baths. Part II—Traditional Operations. W. Adam, jun., and L. B. Rosseau. (Metal Progress, 1950, vol. 57, Mar., pp. 332–334; Apr., pp. 498–501). Advantages and limitations of electric salt baths are discussed. The main features are surface protection, automatic preheat, uniformity of heating, rapidity of heating, and buoyancy. Immersion heating and immersed electrode heating are briefly dealt with. In Part II the techniques for neutral hardening, carburization, and cyaniding are briefly described.—E. T. L.

Carburizing Pastes Permit Fast Low-Cost Selective Hardening. J. Fish. (Materials and Methods, 1950, vol. 31, Apr. pp. 72–74). Two special paste compounds, one for hardening, the other for insulating against hardening, are now available in America under the trade names of Carburit and Isopac. Both have the consistency of putty, and are applied direct to the particular grease-free steel surface. Carburit will produce case depths up to 0·040 in. on heating at temperatures up to 1750°F. Subsequent heat-treatment to refine the grain and harden the case depends upon the type of steel. Isopac retards the entrance of carbon to the surface it covers, and is also left on during the subsequent quench, thereby retarding the rate of cooling. The laborious techniques of selective copper-plating to prevent carburization, or the leaving of surplus stock to be subsequently machined off are now out of date.

Practical Controlled-Atmosphere Furnace Applications Reduce Heat Treating Costs. O. E. Cullen. (Steel, 1950, vol.

126, May 22, pp. 74–76, 100). A recent innovation in heat-treating is a batch furnace capable of producing extremely thin (0·001 to 0·005 in.) file-hard cases on steel parts; very rapid heating is possible, and work from the furnaces is quenched without contact with air. Costs can be reduced as parts come bright from the furnace, no pickling or removal of scale being required. Homogeneous carburizing for steel up to ¼ in. thick can also be carried out.—A. M. F.

Rotary Retort Carburizing Cuts Gas Consumption. A. H. Allen. (Steel, 1950, vol. 126, Apr. 24, pp. 74–76). This is a description of a fully mechanized carburizing plant at the Ford Motor Works, Detroit. Small parts are carburized continuously by passing them through a rotating spiral retort mounted in a furnace. The carburizing gas is introduced at one end of the retort and the tumbling action of the spiral is said to be of considerable value in the case of parts which may retain oil in pockets.—A. M. F.

Martempering of Automotive Gears and Shafts. S. L. Widrig and W. T. Groves. (Metal Progress, 1950, vol. 57, May, pp. 607–612). Details of martempering are given on the basis of **S**-curves and it is pointed out that it causes little distortion, yet involves little sacrifice of hardness. An experimental equipment is described, including a hot oil bath. Tests on the hardness of gears quenched in this apparatus show that it is a practicable process, within certain limits.—E. T. L.

Flame Hardening. E. F. Green. (Canadian Metals, 1950, vol. 13, July, pp. 32–35, 44, 45). An account is given of the characteristics and applications of flame-hardening to cast irons and steels. Suitable types of equipment vary from manually operated welding torches to elaborately controlled machines specifically designed for the purpose. Flame-hardening procedures are described, also methods of quenching, stress relieving, and testing.—E. C.

New Machines for Flame Hardening. H. V. Inskeep. (Steel Processing, 1950, vol. 36, Feb., pp. 91–96). Three modern, fully automatic American flame-hardening machines are described, each one having been designed for a specific class of work. The Cincinnati 'Flamatic' machine will harden the teeth or selected regions of gears, cams, shafts, etc., up to sizes approximately 18 in. in dia. The 'Denver Electronic Universal' machine will surface-harden rolls up to 2 ft. in dia. and 12 ft. long, and harden the teeth of gears up to 6 ft. in dia. The 'Geason' machine is also designed for large-gear hardening. All the machines, and typical examples of their work, are illustrated.—P. M. C.

Continuous Induction Hardening of Cold Finished Steel Bars. J. J. Day, jun. (Metal Progress, 1950, vol. 57, Jan., pp. 69–72). Details of equipment for full cross-sectional hardening of bar stock, and experimental results are given of measurements of the hardness obtained by this and more usual processes. This method produces scale-free products of superior hardness and uniformity of mechanical properties. It is flexible and eliminates subsequent heat-treatment.—E. T. L.

The Carbonitriding Process of Case-Hardening Steel. G. W. Rengstorff, M. B. Bever, and C. F. Floe. (American Society for Metals, Oct., 1950, Preprint No. 1). This is a summary of previous work on carbonitriding, and report of experiments on low-carbon steel (SAE 1020). It was found that the temperature of nitriding had a greater effect than gas composition (NH_3, CH_4 and carrier gas mixture), duration of nitriding, or speed of subsequent quenching on the surface hardness, microhardness, metallography, and case depth. There are 68 references.—E. T. L.

Constitution of Carbonitrided Cases. G. W. P. Rengstorff, M. B. Bever, and C. F. Floe. (American Society for Metals, Oct., 1950, Preprint No. 2). Low-carbon steel (SAE 1020) samples were carbonitrided, and carbon and nitrogen concentrations were then found by chemical analysis; they showed the presence of an outer cementite and ϵ-carbonitride layer, and beneath a layer of martensite and retained austenite. The structural constituents were investigated by X-ray diffraction, and the retained austenite located by lineal analysis. There are 24 references.—E. T. L.

Nickel Plating without Electric Current as a Stop-Off When Nitriding. J. Korecký. (Hutnické Listy, 1950, vol. 5, Oct., pp. 414–416). [In Czech]. The author has carried out experiments on the possibility of nickel-plating without current as a protection for steel surfaces during nitriding. A nitriding chromium–aluminium steel (Cr 1·50%, Al 1·06%) was used. The specimens were 12 mm. in dia and 80 mm. long. After fine-grinding they were nickel-plated for 15, 30, and 60 min. in a bath of a composition as recommended by A. Brenner and G. E. Riddell (*see* Journ. I. and S.I., 1948, vol. 158, Mar., p. 404). This was followed by nitriding for 48 hr. at 490° C. and then the hardness was measured under the nickel layer which was ground off. Plating for 10 to 15 min. in such a bath at 90° C. supplies a sufficiently thick nickel deposit to withstand the action of nitriding at 500° C. for 48 to 96 hr. A bath of a different composition, producing bright nickel deposits was also tested, but the results obtained were less satisfactory.—E. G.

Bright Carbonitriding of Steel Accomplished in Automatic Furnace. H. N. Ipsen. (Materials and Methods, 1950, vol. 31, Mar., pp. 61–63). Carbonitriding is the name given to a special gaseous case-hardening process, in which both hydrocarbon gases and ammonia are used to produce hard carbides and nitrides in the quenched case. These gases are brought to the heated stock by a carrier gas (40% H_2, 40% N_2, and 20% CO) which protects it from oxidation. In the author's firm, an automatic self-contained unit has been built in which the oil-quenching tank is protected by the carrier gas, thus ensuring perfectly clean and bright finishes on the cased articles. Typical examples of work done are quoted and illustrated, case depths of 0·010 in. being obtained in one hour at 1600° F.—P. M. C.

Annealing of Iron Castings. (Hutnik, 1950, vol. 17, July–Aug., Biuletyn Informacyjny Glownego Instytutu Metalurgii i Odlewnictwa, p. 23). [In Polish]. The heat-treatment of pearlitic iron castings is described.—V. G.

Stainless Steel Strip Annealed in Continuous Furnace. W. A. Stermer. (Industrial Heating, 1950, vol. 17, Sept., pp. 1524–1526). This short article describes plant at Washington Steel Corp., Washington, Pa., in which natural gas is used as fuel in the heat-treatment of stainless-steel strip and sheet.—J. A. I.

106-Ton Furnace Charger. (British Engineering, 1950, vol. 33, Sept., pp. 378–380). The machine for charging sheets into the annealing furnaces at the works of John Summers and Sons, Ltd., is described and illustrated. (*See* Journ. I. and S.I., 1950, vol. 166, Sept., p. 95).—R. A. R.

Straight-Line Annealing and Cleaning Cuts Costs. W. H. Holcroft and E. C. Bayer. (Iron Age, 1950, vol. 165, June 8, pp. 87–88). A description is given of an automatic annealing furnace and cleaning line for castings designed and installed by Holroft and Company, Detroit.—A. M. F.

Investigations into the Annealing of Unalloyed and Low-Alloy Steels. S. Ammarellar. (Stahl und Eisen, 1950, vol. 70, May 25, pp. 459–463). The processes for annealing constructional and tools steels to produce good machinability and structure suitable for subsequent hardening are briefly reviewed. The speed of transformation of carbides to spheroidal cementite during the annealing of unalloyed and low-alloy steels with approximately 0·65–1·35% C, particularly roll-neck bearing and unalloyed tool steels, was investigated. The results showed that, by the choice of a suitable annealing temperature, the transformation could be completed in a very short time. The optimum temperature range depends on the carbon content; thus at 0·7% C it is 720–730° C, at 1·0% C it is 730–765° C, and at 1·3% C it is 750–815° C. Annealing periods of 0·5 to 2 hr., followed by controlled cooling to 600° C. and then air cooling, suffice to produce correct structures. Hypereutectoid steels can be heated to well above the Ac_1 point; re-formation of lamellar pearlite occurs only at very high temperatures. The use of a short annealing process developed from these results is illustrated by two examples. J. P.

Ammonia Saves Money at Atmosphere Annealing. L. H. Seabright and N. K. Koebel. (Iron Age, 1950, vol. 165, May 18, pp. 81–85). It is claimed that in the annealing of alloys for electrical use in a hydrogen atmosphere, savings can be made by using dissociated anhydrous ammonia giving 75% hydrogen and 25% nitrogen, instead of commercial hydrogen. Operation of a dissociation plant is described; it is simple in operation, but the initial cost is high, the savings being in reduced handling and storage costs. Comparative figures are given showing that parts annealed in this way come up to the specifications for hydrogen annealing.—A. M. F.

Quenching of Carburized Gears. S. L. Widrig and W. T. Groves. (Metal Progress, 1950, vol. 57, Apr., pp. 482–486). Quenching can cause distortion, and this is considered in an outline of practical details of common methods of quenching.

It is suggested that in open-tank quenching, consistency obtained by mechanization is practical and cheaper.—E. T. L.

Focus on Quenching. R. C. Stockton. (Metallurgia, 1950, vol. 41, Apr., pp. 321–326). A description is given of the various methods of quenching in heat-treatments. The importance of choosing the correct quenching medium for any one job is emphasized, and tables given of the relative quenching properties of various media. Examples are given of the dangers of incorrect quenching practice, as, for example, the irregular nature of quenched specimens which were heavily scaled on immersion, and the explosions which may occur on attempting to quench high-speed steels in nitrate salt baths. Descriptions are also given of recent plants which have been installed and which include various methods of controlling the temperature of the quenching medium, agitation, controlled-atmosphere quenching, induction-hardening quenching, continuous-strip quenching, etc.—J. J.

An Examination of the Quenching Constant, *H*. D. J. Carney and A. D. Janulionis. (American Society for Metals, Oct., 1950, Preprint No. 19). This is a brief review, followed by calculations of H from measured cooling curves, for various steel bars. H increases from centre to surface, and with decreasing diameter, and near the end in an end-quench bar. These effects appear to be due to variations in the surface heat abstraction coefficient, since H is a heat transfer coefficient as defined by Grossman and Asimow.—E. T. L.

Gradual Quenching. B. Karwasinski. (Hutnik, 1949, vol. 16, Nov.–Dec., pp. 452–458). [In Polish]. A short review of the advantages and disadvantages of interrupted quenching and its application to alloy steels used for tools and gauges is given.—W. J. W.

The Tempering of Chromium Steels. R. W. Balluffi, M. Cohen, and D. L. Averbach. (American Society for Metals, Oct., 1950, Preprint No. 20). The second, third, and fourth stages of tempering have been studied in chromium steels by using dilatometric, magnetic, and metallographic techniques, with an electrolytic method of carbide extraction. In the second stage (205–315° C.) retained austenite transforms at a rate accelerated by the presence of tempered martensite. The third stage (above 205° C.) involves forming the high-iron-content cementite, the kinetics of this reaction show that the limiting factor is not carbon diffusion in ferrite. In the fourth stage (starting at 540° C.) $(Cr, Fe)_7C_3$ is precipitated, while cementite redissolves. This carbide is coarse and spheroidal, and hardly inhibits plastic deformation. Chromium retards softening by restricting the growth and spheroidization of the cementite platelets in the third stage. There are 22 references.—E. T. L.

Martempering of K-46 Tool and Die Steel. G. L. Kehl. (United States Atomic Energy Commission, 1946, MDDC 526). The martempering of K-46 die steel (C 0·85–0·95%, Si 0·25–0·45%, Mn 1·00–1·25%, Cr 0·40–0·60%, W 0·40–0·60%, V 0·10%) having an initial spheroidized structure can be carried out as follows: (*a*) Austenitize at 1475° F., (*b*) quench from 1475° F. in a salt or lead alloy bath at 500° F., (*c*) hold at 500° F. for a time depending upon the size of piece, but in no case for longer than 3 min., and (*d*) cool from 500° F. in air. The process is not applicable to sections thicker than about 1 in.—R. A. R.

FORGING, STAMPING, DRAWING, AND PRESSING

Transverse Mechanical Characteristics in Forgings. A. Bartocci. (Metallurgia Italiana, 1950, vol. 42, Aug.–Sept., pp. 289–296). With large and important forgings quality control is usually achieved by cutting out samples in a direction transverse to the strain lines. The quality characteristics in a direction transverse to strain show very considerable differences, the reasons for which are not always known. This paper examines, largely by statistical methods, the various factors which affect these characteristics in special steel forgings. Amongst the factors examined are the effects of working, the shape of the ingot, the forging procedure, and the type of steel, whether made in the acid open-hearth or basic electric furnace.—M. D. J. B.

Increasing the Speed of Forging. F. Drastík. (Hutnické Listy, 1950, vol. 5, June, Supplement No. 2, pp. 80–83). [In Czech]. Possibilities of speeding up forging operations are discussed. Since the total deformation work is the sum of the partial deformations occurring during each blow, forging can be speeded up by increasing the number of blows in unit of time. In practice the speed limit has been reached for present-day single-hammer machines and the only way to increase the forging speed is by introducing machines with several hammers striking the piece in succession. In the case of a bar, further shortening of the forging time can be obtained by placing the second pair of hammers at 90° to the first pair. By using four hammer units in succession the forging time can be reduced by 75%. Such units would be particularly advantageous for forging high-alloy steels, and the increased speed would in some cases eliminate reheating between successive forging operations.—E. G.

Technical Progress in Forging Equipment. R. E. W. Harrison. (Steel Processing, 1950, vol. 36, Apr., pp. 177–182). This article records some of the more important elements of progress developed during recent years in the techniques of hot shaping of metals. Nasmyth's original steam hammer is illustrated, and the performances of this and subsequent models are tabulated. The development of mechanical upsetters and forging presses is briefly outlined.—P. M. C.

Production of Forgings. W. Naujocks. (Steel, 1950, vol. 126, Mar. 13, pp. 106–116; Mar. 20, pp. 91–103). This paper presents a discussion of the various types of forging press, heating unit, and cleaning equipment used in the American forging industry.—A. M. F.

Fast Heating Improves Forgeability. C. A. Turner, jun. (Steel, 1950, vol. 126, Mar. 20, pp. 76–79, 103–106). Extensive laboratory tests by the Selas Corp. of America have shown the improvements in forging that may be obtained from heating the steel stock more rapidly than the conventional rate. Tests have shown an increased penetration of 23% in 0·40% carbon steel. This is believed to be due to the decreased formation of subsurface intergranular oxides reducing the internal friction, and the finer grain size associated with faster heating. Reduced scaling and decarburization are among the other benefits obtained.—A. M. F.

What Price Scale! H. L. Showalter, jun. (Steel Processing, 1950, vol. 36, Mar., pp. 143–149). The advantages of scale-free heating before hot forming are discussed in some detail. Emphasis is laid on the financial saving due to less metal wastage, less wear on forging tools, fewer rejects due to poor surface condition, reduced cleaning and pickling costs, and reductions in finish-machining.—P. M. C.

Cold Roll Forming. E. J. Vanderploeg. (Steel, 1950, vol. 126, Apr. 24, pp. 81–84; May 1, pp. 91–93). A general description of cold-roll forming is given, stress being laid on the provision of an adequate number of passes to ensure close tolerances and freedom from defects. This forming operation will always result in lower unit cost provided quantities are big enough to warrant the initial outlay.—A. M. F.

Rolled and Forged Circular Parts. (Steel Processing, 1950, vol. 36, Feb., pp. 84–86). The article briefly describes the unusual method of producing circular blanks for wheels, gears, flywheels, etc., at the Bethlehem Steel Company. A block of heated steel is placed between two circular dies, one vertical and the other inclined at a slight angle to the vertical. The contours of opposite sides of the article to be produced are cut in the two dies which are pressed together and rotated in the same direction and at the same speed. The blank is thus shaped by a combination of forging and circular rolling. The parts, which are nearly all made of alloy steel, are quenched and tempered under carefully and automatically controlled conditions.—P. M. C.

Willys Simplifies Forging Inspection. G. H. Fromer. (Iron Age, 1950, vol. 165, June 22, pp. 95–97). A Magnaglow machine is used for inspection at the rate of 600 pieces/man-hr.; with this the fluorescent magnetic particles are viewed under black light to reveal flaws. The main feature is the use of a continuous conveyor system.—A. M. F.

Efficient Wrench Forming. T. A. Dickinson. (Steel Processing, 1950, vol. 36, June, pp. 285–287). The author describes the sequence of operations in the production of box and open-end wrenches at the Plomb Tool Co., and at Thorsen Tools, both of California. The former company relies on Erie board hammers, and the latter on 100-ton Ferracute and Vernon punch presses.—P. M. C.

Prejudging Low Carbon Drawing Quality Sheets. E. F. Lundeen. (Pressed Metal Institute: Steel Processing, 1950, vol. 36, Apr., pp. 186–188, 213). The melting, hot and cold rolling, and final annealing of a 100-ton batch of steel sheets is

a complex process. To carry this out in an economical manner means that variations in the properties of the final sheets cannot be entirely avoided. The metallurgists' microscopic, cupping, hardness, and tensile tests are not the same as an intricate drawing operation, and therefore cannot indicate the suitability of a sheet for deep drawing unless they are compared with similar tests on material which is known to draw satisfactorily. The only way to judge a drawing-quality sheet is in the press itself.—P. M. C.

Tungsten Carbide Tooling for Cold Heading. Part III. W. E. Montgomery and W. Leigh. (Steel Processing, 1950, vol. 36, Mar., pp. 138–141, 152, 153). Conclusion of a series of articles (*see* Journ. I. and S.I., 1950, vol. 164, Apr., p. 491). This final article describes typical cut-off quills and knives, used in the production of uniform lengths of wire. The dimensions of these tools and their respective carbide inserts are tabulated for various wire sizes and for different types of machine.—P. M. C.

New Forming Process Demonstrated. J. C. McComb. (Steel Processing, 1950, vol. 36, Feb., pp. 80–83). The Marform deep-drawing process has been publicly demonstrated and made available to industry by the Glenn Martin Co. (U.S.A.). Standard hydraulic presses can be adapted for the new process, which is based on the use of rubber as a substitute for the usual female die. This die is mounted on the top platen, and lowered on to the metal blank supported on the lower platen. The steel male punch then moves upwards through an opening in the lower platen, and pushes the metal into the female rubber die. A special hydraulic unit moves the male punch, and the pressure during this stroke can be varied to meet the requirements of the particular draw. Experiments on steel, aluminium, and some stainless steel have showed that deeper drawing can be accomplished in one operation of the Marform process, than by normal methods. Finally, the principles of the process are compared with those of the Guérin process, from which the Marform was developed.—P. M. C.

9 Operations on 2 Presses Cut Stamping Costs of Toaster Shells. W. F. Ardussi. (Steel, 1950, vol. 126, May 15, pp. 79–81). A 3¼-in. draw as one of a number of operations in a press equipped with air cushions is achieved by the use of equalizer pins to equalize the pressure over the bed of the press. The material used is 0·032-in. chromium-plated cold-rolled steel strip. The correct selection of a drawing compound was found to be a critical factor in reducing breakage and wrinkling of the drawn parts.—A. M. F.

Stainless Steel Fabricating Costs Cut $100 per Day. A. P. Schulze. (Steel, 1950, vol. 126, May 8, pp. 82–84.) The United Refrigerator Co., Hudson, Wis., is using a strippable plastic coating to protect stainless-steel sheets from abrasion and scratching during fabrication. In addition die life is increased due to the coating's cushioning effect.—A. M. F.

Quality Stainless Wire Produced in Quantity at New U.S. Steel Division. (Industrial Heating, 1950, vol. 17, Sept., pp. 1508–1520). A full description is given of a new mill at the Waukegan, Illinois, works of American Steel and Wire Co., designed to process rounds up to ½ in. in dia., and certain sizes of flat stainless-steel wire. The processes described include annealing, cleaning, coarse and fine drawing, coating, straightening and cutting, grinding, and cold rolling. The plant has a rated capacity of 500 tons/month.—J. A. L.

American Steel and Wire Company Opens Stainless Division at Waukegan. (Association of Iron and Steel Engineers Yearly Proceedings, 1949, pp. 450–453). The new wire mill at the Waukegan Works of American Steel and Wire Co., is described (*see* preceding abstract).—R. A. R.

If You Make Plated Parts, You Can Save with This New Method of "Prepolishing" before Forming. A. H. Allen. (Steel, 1950, vol. 126, Apr. 17, pp. 76–77, 94). The need for tedious hand polishing of pressed steel parts after forming but before plating to remove die scratches and other defects has been removed by machine polishing of steel in the flat, coating with a water-soluble and thermosetting chemical film, press-forming, washing off the film, and then plating.—A. M. F.

Ford Extrudes Axle Sprinkles. H. Chase. (Iron Age, 1950, vol. 165, June 15, pp. 108–109). A single cavity extrusion die is being used instead of a steam forging hammer and open dies. Advantages claimed are 30% reduction in scrap, lower die costs and substantial labour savings; in addition the fibre flow approximates to the ideal and fatigue resistance is increased.—A. M. E.

Cold Extrusion of Steel. J. P. Bowden. (Steel Processing, 1950, vol. 36, May, pp. 227–233). Cold extrusion has been successfully developed since the second world war by the Heintz Manufacturing Co. (U.S.A.). This success is mainly due to the use of a phosphate bonding agent between the steel and a suitable lubricant. A complete schedule for the cleaning, coating and lubrication of the surface, known as the Foslube Process, has been developed by the Pennsylvania Salt Manufacturing Co. Both forward and backward extrusion have been satisfactorily accomplished on the following steels: C1010, C1019, C1020, A4615, A5120, and type-304 stainless. The effect of up to 80% reduction during extrusion, on the tensile strength and elongation, is also recorded. The experimental production of 20-mm. and 75-mm. shell bodies, 105-mm. howitzer cartridge cases, rocket motor bodies, and burster casings is fully described. The author emphasizes the great economic advantage of the process over machining methods, since the final article often weighs 95% of the original billet. Scrap losses are thus very low.—P. M. C.

Cold Drawing. L. Sanderson. (British Steelmaker, 1950, vol. 16, Mar., pp. 128–131; Apr., pp. 184–187; June, pp. 310–313; July, pp. 364–367; Aug., pp. 421–424). The principal features of the cold drawing of steel bars are described in the first part. In the second and third parts successive operations in the cold drawing of wire and dies for wire drawing are dealt with. A metallurgical explanation of the increase in hardness and tensile strength conferred by cold drawing is put forward in the fourth part. Three methods of cold drawing tubes and the drawing of steel sheets are described in the last part.—R. A. R.

Theoretical Aspects of Wire Drawing. P. N. Richards. (Australian Institute of Metals: Australasian Engineer, 1950, May 8, pp. 68–76). This is a survey of the more important theories of wire drawing. The aim of most investigators has been to deduce a formula for the drawing force, which depends upon the mechanical properties of the metal, the absolute size and reduction of area, the shape of the die, and the coefficient of friction. Experimental results, applied to the various theoretical formulæ, have given values for the coefficient of friction which vary from 0·05 to 0·30. The author concludes that none of the present theories is completely satisfactory, nor does it appear likely that one will be derived until the recent complex ideas of plastic deformation are considered together with more precise information as to the true practical value of the coefficient of friction.—P. M. C.

Continuous Wire Drawing—New Barron and Crowther Machine. (Wire Industry, 1950, vol. 17, Aug., pp. 657–658). A new non-slip multi-hole wire-drawing machine is described. A prototype 'three-holer' has been in operation for several months on 5 SWG mild steel and 0·6% carbon steel wire. The machine has conventional electrical gear but with a novel method of accumulation. Instead of the conventional wapping-off arm and ring guide working off a single tall block, the unit consists of two blocks of which the upper is free to rotate on the spindle in either direction. The wire is drawn by the lower block which accommodates about one half of the total quantity of wire stored between passes. At the top of the lower block is situated a pulley which transfers the wire to the upper block. The pulley is mounted on a friction band, and the wire is wound on the upper block in the opposite direction to the lower one. If wire is taken off at a higher rate than it is being accumulated then the upper block will revolve faster than the lower one, and the transfer pulley will revolve in the direction opposite to that of the lower block at a speed of one half of the difference in speed of the two blocks. It follows that the stores of wire on the upper and lower blocks will be depleted at the same rate. Three advantages are claimed: (1) Since the wire is always unwound tangentially, twist is eliminated and the machine is suitable for drawing shaped wire. (2) The machine may run continuously whereas a separate coiling block is stopped for stripping. (3) The wire may be wound on to spools running at constant angular speed; as the spool builds up, and therefore winds at higher rate, it depletes the reservoir of wire on the last twin block; the reservoir is replenished while spools are being changed.—J. G. W.

New Italian Wire-Drawing Machine. (Wire Industry, 1950, vol. 17, Sept., pp. 747–748). A multihole, straight line, capstan wire-drawing machine, invented by 'Metallogen' S.A. of Milan, is now being manufactured for the general

market. Photographs of the machine, called 'Mill,' are shown and performance figures are given. The problem of speed matching and wire-tension control is solved in a novel fashion which, it is claimed, results in remarkably high transmission efficiencies (up to 95%); it is suitable for slip, non-slip, and back-pull methods of drawing, and permits of unit construction. Each capstan is driven by a constant-speed A.C. motor through a differential and a set of interchangeable gears. With the aid of these the capstan speed is matched as closely as possible to the wire speed. The final adjustment is done automatically by the differential, which is prevented from idling by a mechanical or hydraulic brake absorbing a torque proportional to the load on the capstan. To minimize transmission losses the brake should be as nearly as possible stationary, and this in turn depends on the selection of the reduction gears; up to 44 different speed ratios are available. Typical performance data obtained on a 3-hole machine are quoted. It draws 0·220 in. 0·08–0·12% carbon steel wire of 25–30 tons/sq. in. tensile strength down to 0·120 in. or 0·108 in., finishing at 600 or 675 ft./min. and with an output of ½ ton/hr. Each unit of the machine is powered by a 3-phase A.C., 14·5-h.p. motor running at 1450 r.p.m.—J. G. W.

Carbide Wire Drawing Dies—Bearing *vs.* No Bearings. J. O. Tompkins. (Wire and Wire Products, 1950, vol. 25, July, pp. 576–578). The author has compared the performance of dies, with and without cylindrical sizing portions, under actual production conditions. Six 'no-bearing' and six 'bearing' dies were used on continuous drawing machines as finishing dies during regular production of 0·038-in. tyre bead wire (0·74–0·80% C and 0·55–0·65% Mn), the reduction in area being about 20%, and the drawing speed 1200 ft./min. The profiles of the dies were determined by casting powdered Bakelite (4000 lb. pressure at 300° F.) in an ordinary moulding press and projecting enlarged images of the castings on to a screen from which measurements were made. The die profiles were thus measured when new and after 500,000 ft. of wire had been drawn. The dies were then repolished to the smallest size possible for use and the enlargement was recorded. In every case the 'bearing' dies produced darker wire which indicated better lubrication, and presumably less die wear. The 'no-bearing' dies enlarged between 0·0006 and 0·0011 in. (average 0·0008 in.) after drawing, and the average diameter after repolishing was 0·0021 in. larger than the original. The corresponding figures for the 'bearing' dies were: 0·0001–0·0004 in. (average 0·0003 in.) increase after 500,000 ft., and 0·0011 in. as a result of repolishing. Apart from more rapid wear the 'no-bearing' dies led to out-of-roundness and scratching of the drawn wire. There was no difference in the properties of the wire.—J. G. W.

Symposium on the Use and Care of Diamond Wire Drawing Dies. (Industrial Diamond Association of America: Wire and Wire Products, 1950, vol. 25, Feb., pp. 145–146, 156–157). Factors are enumerated which prolong the life of dies and therefore reduce die costs. Frequent inspection should remove dies before they have exceeded the size tolerance. The insertion into the train of over- or undersize dies is to be deprecated and one should aim to remove dies by sets. If the dies are removed in time they can be repolished so that full advantage is taken of the stone, otherwise the stone may have to be discarded or the fault developed in the first service cycle shortens all subsequent ones. Some typical examples of the abuse of diamond dies are given, also illustrations of profiles recommended for different application. A 9° drawing angle is, for instance, recommended for high-carbon steel, 12° for bronze, 14° for Monel and brass, and 18° for copper and aluminium. Carbide and diamond dies should not run together because the former will wear more quickly, thus throwing the strain on to the latter. Factors contributing to poor die life are inclusions in the rod, poor pickling, varying heat-treatment, using one lubricant for drawing different alloys, and incorrect slip on the cones.—J. G. W.

An Investigation of the Weavability of Wire with Observations on the Defects of Wire and Wire Cloth. W. Chubb, jun. (Wire and Wire Products, 1950, vol. 25, Feb., pp. 125–129, 174–177; Mar., pp. 222–225, 248; Apr., pp. 300–310, 329–336; May, pp. 407–411, 453). Work carried out under the Ludlow-Saylor Company, St. Louis, Research Fellowship is reported in four instalments. The first contains historical notes on the wire-weaving industry from biblical times to the formation of the Ludlow-Saylor Company, a description of wire weaving and of tests commonly used, and concludes with the coining of the terms weavability, workability, and formability. In Part II the author describes the transverse-crimp-sensitivity test developed by himself and Eppelsheimer (*see* Wire and Wire Products, 1949, vol. 24, June, p. 491), in which the crimping load is proportional to the wire diameter squared. The factor of proportionality is related to the secant modulus and affords a measure of the ease with which wire is crimped. Part III is concerned with defects commonly encountered in weaving wire. These, the author finds, are invariably caused by annealing faults. In several cases the annealing was confined to one side of the wire coil. Another cause of improper structure was banding and segregation. The author concludes that more complete specifications are required for weaving wire. Part IV contains a summary, recommendations for further work, a glossary, 37 references on general topics, and an indiscriminate 'bibliography' of papers on wire and allied subjects.—J. G. W.

The Dependence of the British Standards Reversed Bend Test for Wire on the Testing Machine. J. G. Walford and J. G. Wistreich. (Wire Industry, 1950, vol. 17, Aug., pp. 663–671). Two types of machine for performing the reversed bend test on wire, the Denison Model T.22BW and the Tarnogrocki instrument specified in the German Standard DIN.51211 (1934), have been examined. Two models of the latter design were used. In the main tests six grades of wire were used, two for each of three sizes near the middle of the range of diameters specified for rolls of 2·5, 5·0, and 10·0 mm. in B.S. 163 (1943–45), B.S. 329 (1939), B.S. 366 (1929), and B.S. 3W1 (1927). Twenty-four tests were performed on each of these grades in the B.I.S.R.A. and Denison machines and the results were compared by scatter diagrams. In other experiments the behaviour of the wire specimen during the test was studied in order to establish the cause of kinking, twisting, and of other faults encountered in the reversed bend test. Both the design and workmanship of the machines have a pronounced effect on the number of bends to fracture. The most important design factors appeared to be the presence or absence of a clearance between guide rolls and wire, and the position of the rolls relative to the bending arm. With regard to the precision of manufacture the smoothness of the guide rolls and their symmetrical disposition about the bending arm and about the guide holes provided in it, seemed to be important, if results obtained on one machine are to be repeated on another. Both types of machine graded wire in the same order, and tests on adjacent samples were repeatable to the same degree. The differences between the B.I.S.R.A. and new Tarnogrocki machines ranged from 3 to 12%, between the B.I.S.R.A. and Denison machines from 30 to 100%. On the strength of the results obtained, a standardization of the important features of the testing machine is recommended by the authors. No specific design is proferred but it is suggested that the Tarnogrocki dies might well be combined with the Denison jaw movement. The shapes of the die profiles and the location of dies and guide holes should be specified.
J. G. W.

Borax as a Coating for Drawing High Carbon Steel Wire. C. Voigtlander. (Wire and Wire Products, 1950, vol. 25, Aug., pp. 656, 684–685). The use of borax as a coating for patented high-carbon rods and wires in the Union Wire Rope Corporation, Kansas City, is described. Rods are cleaned in 4½% (by volume) acid at 150° F., and wire in 4% acid at 140° F. Pickling time is 20 min. with agitation after about 10 min. The coils are then washed and sulled for 4 to 7 min., which is followed by a dip in borax solution at 190° F. This solution contains 10–11 oz./gal. The coils are dried in flash bakers at 600–650° F. Acid and borax strengths are tested every 2 hr. The borax solution ages through accumulation of iron oxide. This is settled out by cooling. The lubricant used is calcium stearate with a small amount of soda soap. For furniture spring wire sull coating is left out and a 30% mixture of lime and soap is placed in the first die box. All fine wire is still limed for wet drawing.—J. G. W.

Production, a Series of Compromises. L. J. Lovisek. (Wire and Wire Products, 1950, vol. 25, July, pp. 563–567). The author calls attention to compromises that can be made to keep production high and costs low in cold-heading industry. When making a common headed screw, the cheapest, simplest and most effective punch is the 'solid' type consisting of three parts only: the body, the backing plug, and the pin. This punch calls, however, for wire 0·0005 to 0·0010 in. smaller

than the diameter of the hole in the heading die. Unfortunately bright basic steel wire for cold heading has a much larger tolerance than this and is usually out-of-round. The choice will be dictated by a compromise between costs of material and of tools. The choice of the tool also depends on the cold-heading properties of the material. Bright basic low-carbon wire is self-supporting for 1·5 times the wire diameter, whilst an 18/8 stainless steel will buckle, if the free length exceeds 0·75 times the wire diameter.—J. G. W.

New Rod Baking Ovens at Seneca Wire Are Modified Flash Bakers. (Industrial Heating, 1950, vol. 17, Apr., pp. 669–674). This article describes the installation of two rod-baking ovens at the Seneca Wire and Manufacturing, Co., Fostoria, Ohio. Details are given of the oven construction, heating and recirculating equipment, control technique, and operating data. Each oven is operated as a batch-type unit, giving a production of 17,000 lb./hr. of lime-dipped steel rod for wire-drawing. In addition the ovens are used for baking aluminium rod.—J. A. L.

American Wire Mill Equipment. K. B. Lewis. (Wire and Wire Products, 1950, vol. 25, June, pp. 495, 532–533). This is a review of an article by H. Höhle entitled "Experience and Development in the American Wire Industry since 1939" which appeared in Stahl und Eisen, 1950, vol. 70, Jan. 19, pp. 41–51 (*see* Journ. I and S.I., 1950, vol. 166, Nov., p. 264).

Nail Production. G. K. Rylands. (Wire and Wire Products, 1950, vol. 25, June, pp. 487–491, 524–529). A detailed description is given of the layout and operation of the new Nail Department of Rylands Brothers, Ltd., Warrington. In rebuilding the department the author and his associates concentrated on its layout and organization. No new machines were acquired yet the output was raised from 600 to 800 tons/week with only 116 men employed as against the original 175. This improvement was achieved by eliminating unnecessary handling and an efficient signalling system which enables the foreman to deal quickly with hold-ups and deploy hoists most effectively. Wire of 8 gauge or thicker is supplied to the plant in 1-ton weights, and the finished nails are handled in 1-ton skips. The nail machines are placed in rows, with two rows discharging nails into skips placed in a long trench. As soon as the skip is full, the machine operator stops the machine which automatically switches on a signal calling for the skip hoist and notifying the foreman.—J. G. W.

ROLLING-MILL PRACTICE

The Layout of Rolling Mills: Importance of Good Planning. W. Bailey. (Iron and Coal Trades Review, 1950, vol. 160, May 19, pp. 1115–1125). The author discusses the importance of planning the layout of rolling mills. The different types of mill are described and typical modern layouts are illustrated, and it is emphasized that the finishing end of the mill must have adequate capacity. There must also be ample heating capacity, crane power, and storage space, with good lubrication and maintenance. The mill should be designed for a specific range of products, and flexibility and avoidance of cramping are desirable in order to obtain maximum efficiency. G. F.

Work and Power in Rolling. Z. Wusatowski. (Prace Badawcze Glownego Instytutu Metalurgii i Odlewnictwa, 1950, No. 3, pp. 213–267). [In Polish]. Theoretical considerations of the methods used for calculating work and power in rolling are given. All factors affecting this problem are taken into account, *i.e.*, work and power necessary for plastic deformation of metal in rolling, moment of additional friction (losses caused by friction on necks and in all gears during rolling), moment of idle running (energy necessary for running without load), and dynamic moment (energy necessary for acceleration or for retardation of all rotating masses). Practical ways of calculating work and power in hot- and cold-rolling processes are discussed. Many data and diagrams from the literature are collected, and numerous examples of their practical application in calculation are given. V. G.

The Deformation Capacity of Steel. S. Todl. (Hutnické Listy, 1950, vol. 5, June, Supplement No. 2, pp. 85–97). [In Czech]. The conditions inside steel during hot rolling were investigated. The laws of motion of fluids of very high viscosity are applicable to the deformations which occur during the hot-shaping, rolling, and forging of steel. Whilst during cold-shaping the deformation velocity need not be taken into consideration when internal stresses are calculated, at high temperatures the shear stresses in the slip planes are proportional to this velocity. On applying to hot-shaping the conceptions applicable to the motion of high-viscosity fluids it becomes evident that both compressive and tensile stresses are present during forging, which is contrary to the generally held view that during forging the material is only subjected to compressive stresses and that any cracks are therefore welded up. Numerous tests proved that this is not so. In one case rivets were placed in holes drilled in parts to be shaped in order to determine under what conditions internal hollow spaces are eliminated by shaping. Several other tests also showed that tensile and compressive stresses occur simultaneously in hot-shaped metals. A higher content of carbide-forming elements and more impurities are frequently the cause of intercrystalline fractures in large forgings. Acid steels are more inclined to form cracks than basic steels. The most suitable method of determining the hot-shaping capacity of a metal is the torsion test without any simultaneous axial tensile stress. Owing to the low strength of the steel at high temperatures, intercrystalline fractures may arise even during the hot-shaping process and also while the workpiece cools down, since impurities, carbides, and nitrides influence the behaviour of the grain boundaries. Similar intercrystalline fractures were also observed in articles of thin sheet welded at high temperatures; in these tensile stresses were predominant. Heat-treatment has a favourable influence only if carbide precipitation is prevented and if the material contains vanadium and titanium. The difficulties in producing large forgings are also due to the exacting mechanical properties specified. This calls for steels high in carbon and alloying elements which have a stronger inclination to develop intercrystalline fractures. The solution lies in adopting designs which incorporate several smaller forgings, replacing forgings by castings, welding forgings to castings, or using completely welded structures.—E. G.

The Calculation of the Diameter of Grooved Rolls for a Rolling Mill. C. Murski. (Hutnik, 1950, vol. 17, July–Aug., pp. 207–217). [In Polish]. New formulæ for calculating roll diameters and examples of their application are presented. V. G.

Recent Developments in Blooming, Bar and Billet Mills. A. F. Eisel. (United Effort, 1950, July–Aug., pp. 3–7; Sept.–Oct., pp. 3–5). The new plant recently installed at the Lorain Works, National Tube Company and constructed by the United Engineering and Foundry Company is described. P. C. P.

Rolling Mills and Presses—Some Plant Recently Completed or under Construction. (Metallurgia, 1950, vol. 42, July, pp. 86–90). Examples are given of recently designed and installed rolling mills and presses in Great Britain. These include both general and specialized equipment. Examples are given of blooming and slabbing mills, also of multi-stand and cross-country units for high production rates. The development of single-purpose mills in general is mentioned. Examples are given of auxiliary mill machinery such as flying shears, roll straighteners, and manipulators. The proposed versatile and heavy-duty forging presses being constructed for a firm are given special mention.—J. J.

New Rolling Mill Plant at the Normanby Park Steelworks of John Lysaght's (Scunthorpe Works) Ltd. A. H. Norris. (Lincolnshire Iron and Steel Institute, Feb. 21, 1950). A detailed account of the soaking pits and rolling mills at John Lysaght's (Scunthorpe Works) Ltd., which is part of the post-war reconstruction plan for the whole works, is given. The soaking pits consists of 12 one-way-fired pits in three batteries of four cells each. These cells will each take 18 3½-ton ingots. The blooming mill is of the two-high reversing type with rolls 42 in. in dia. with a barrel length of 102 in. driven by a 6650-h.p. motor. The Morgan billet and sheet-bar mill comprises a group of continuous roll trains consisting of six horizontal stands, the first four with rolls 21 in. nominal dia. and the last two 19 in. in dia. There are also two 16-in. vertical edging mills. Great attention was given to facilities for roll turning and general roll maintenance.—R. A. R.

Forty-six Years of "Know-how" Built into the New Colorado Fuel and Iron Rod Mill at Pueblo. (Wire and Wire Products, 1950, vol. 25, Mar., pp. 211–215). A new rod mill has been installed in the 46-year-old Pueblo plant of the Colorado Fuel and Iron Company, costing five million dollars. The

new buildings occupy 87,500 sq. ft. The billet-handling equipment and reheating furnaces are described. The billets are broken down to 2½ in. square in a four-stand mill, and reheated in the second furnace. This is followed by a three-strand, twenty-three stand Morgan rod mill finishing the rod at 156 m.p.h. and reeling it on six coilers into 700-lb. bundles. The mill is painted according to a special safety colour scheme with battleship grey for the machinery, green for structural steel at eye level, blue on hot surfaces, yellow for 'caution' and red for 'danger.' The mill is operated from a booth high above the mill floor; reel operators have control desks near the coilers. A loudspeaker system can be operated by each operator from his desk. The finished rod cools off on a 900-ft. long hook conveyor and thence is transported to the wire mill by an electric shuttle car. The old rod mill produced 165-lb. bundles. To cope with the increased output the wire mill has been equipped with a battery of 14 Vaughn continuous wire-drawing machines.—J. G. W.

Rejects in the Manufacture of Hot-Rolled Semi-Finished Products. G. Temesszentandrási. (Bányászati és Kohászati Lapok, 1950, vol. 5, Oct., pp. 566–570). [In Hungarian]. The quantity of rejected hot-rolled steel which is scrapped in Hungary is still very high. The author discusses the causes of scrap from the roughing stands; this accounts for about 70% of the total scrap of the rolling mill. Rejects due to twisting of the rolled material amount to about 36% of all the rejects from the mill. It was found that with cast steel rolls the rejects due to twisting increased from about 0·36% after rolling 10,000 tons to about 0·41% after rolling 20,000 tons of steel, and after that they rose very rapidly. Considerably better results were obtained with forged steel rolls. The rejects due to overheating and inadequate heating, and prospects of improvement are also discussed.—E. G.

Production of Merchant Shapes. F. S. High. (Steel, 1950, vol. 126, May 8, pp. 94–106). The author describes the various types of mill used today in America for rolling merchant shapes together with typical pass arrangements for the rolling of different sections.—A. M. F.

Modern Metallurgical Trends in the Production of Steel Plates. W. Barr. (Presidential Address, West of Scotland Iron and Steel Institute, Oct., 1950). The following are among the trends in the production of steel plates discussed: (1) A demand for plates of increasing width (up to 120 in.); this will probably lead to the construction of a 4-high plate mill in Scotland. (2) The improvement of the notch ductility of plates; this is promoted by having a minimum manganese content of two and a half times the carbon content, and by a low finish-rolling temperature. The notch ductility of thicker plates finishing at high temperature can be improved by (*a*) normalizing, (*b*) a further increase in the manganese/carbon ratio, and (*c*) using a special grain-controlled steel. (3) The use in Sweden of a grain-controlled steel (C 0·15%, Mn 1·0%, A.S.T.M. grain size 6–8) with ultimate tensile strength 28–32 tons/sq. in., yield point 17 tons/sq. in., elongation 25% min. on 8-in., V-notch Charpy impact strength 35 ft.lb./min. at −10° C. (4) The rolling of boiler plates 4 and 5 in. thick of a special silicon-killed fusion-welding quality. The present acceptance specifications for such plates are criticized.—R. A. R.

Elastic Deformation of Rolls during Cold Rolling and Its Influence on the Rolling Process. W. Nowakowski. (Hutnik, 1950, vol. 17, Jan.–Feb., pp. 7–10). [In Polish]. A study of the elastic deformation of rolls during cold rolling and its influence on the rolling process is presented. The rolling of very thin foils and strips with rolls made of sintered carbides is discussed.—W. J. W.

A Photoelastic Dynamometer for Rapidly Varying Forces. E. Orowan, F. H. Scott, and C. L. Smith. (Journal of Scientific Instruments, 1950, vol. 27, May, pp. 118–122). A photoelastic dynamometer applicable to the measurement of rapidly varying forces is described. It consists of a glass slab, upon which the forces act, placed between two polarizers; the intensity of the light transmitted is a measure of the force. It is indicated by a vacuum photocell in conjunction with an A.C. amplifier and a cathode-ray oscillograph. Theoretical analysis, design details, and specimen distribution diagrams are shown. The arrangement has been used for recording the variation of pressure over the area of contact in rolling-mill research.—H. D. W.

Defects in Rolled Steels. Z. Remport. (Bányászati és Kohászati Lapok, 1950, vol. 5, Sept., pp. 513–517). [In Hungarian]. The defects which occur in rolled steels are discussed particularly as regards conditions in Hungary. About 22% of these are such that they are detected during the shaping and such pieces are scrapped; the most serious of the hidden defects is lamination in boiler plates. A serious difficulty is that the steel is not always gripped properly by the rolls, particularly in the case of tool steels which are rolled at lower temperatures than ordinary mild steel. Further reasons for defective material are: (1) Material adhering to the rolls; (2) inaccurate assembly and alignment of roll stands; and (3) incorrect roll calibration. In Hungary forged steel rolls are used for roughing stands and cast iron and steel rolls for the finishing stands. The supply of rolls has improved in Hungary, but it is still not satisfactory. Good results have been obtained there with cast steel rolls containing C 0·6–0·7% and Cr 1–2%. Incorrect heating also causes defects in rolled sections. Decarburization is particularly intensive in the case of high-carbon steels and the author recommends the removal of the surface layer of the semi-finished material before preheating, to remove the decarburized layer already present.—E. G.

Steel Rolling Costs—Here's a Method of Reducing and Controlling Them. D. R. Clements. (Steel, 1950, vol. 126, May 15, pp. 92–98). Cost control reports are furnished to foremen in all the main finishing departments on a daily basis so that corrective action can be taken as soon as it is apparent that it is needed. The information is provided in operating rather than accounting terminology; it is simple, accurate, and reflects both favourable and unfavourable results in such a way that the cause or individual is clearly indicated.—A. M. F.

MACHINERY FOR IRON AND STEEL PLANT

The Maintenance of Hydraulic Valves. J. H. Dotter. (Iron and Steel Engineer, 1950, vol. 27, Sept., pp. 124–128). This paper is devoted to the care and maintenance of hydraulic operating valves and contains brief information on leather packing, and washers, how to eliminate valve-stem binding, and rebushing of chamber walls. Good design features permitting easy replacements are also considered.—M. D. J. B.

Emergency Rebuilding of Hot Metal Charging Crane. A. D. Howry. (Iron and Steel Engineer, 1950, vol. 27, Sept., pp. 120–121). A description is given of an emergency rebuilding of a hot-metal charging crane at the Alan Wood Steel Co. The work consisted of stripping the crane, reboring the kingpin bushings, aligning the bridge, and replacing rivets. The auxiliary trolley was removed and completely rebuilt, all worn wheels, gears, bearings, sheaves, etc., were replaced. Within 24 hr. the work was well in hand and ten days later the crane was again in operation.—M. D. J. B.

Repairs to Heavy Castings. G. O. Van Artsdalen. (Iron and Steel Engineer, 1950, vol. 27, Sept., pp. 122–123). The author describes the cold method of repairing broken machine sections. The method presents certain advantages in cases where repairs to cast iron by welding might prove difficult or cause distortion. Lacing by the locked metal process, and the inlay of high-tensile steel bars across the break are discussed and actual repairs described.—M. D. J. B.

Bearing Repair and Inspection. C. T. Lewis. (Iron and Steel Engineer, 1950, vol. 27, Sept., pp. 56–79). The necessity for adequate inspection and maintenance repairs to large bearings is stressed. Details are given concerning the transport of bearings to the repair shops, methods of protection, cleaning, and lastly, grinding and finishing for refitting. Types of fault and methods of curing these are also discussed. M. D. J. B.

More Power For Bethlehem's Sparrows Point Plant. J. M. Spencer. (Iron and Steel Engineer, 1950, vol. 27, Aug., pp. 108–115). A description is given of the new 75,000-kW. power station designed to meet the new requirements of Bethlehem's Sparrows Point Plant. The two boilers are fired with blast-furnace gas, coke breeze, and oil, and are of 400,000 lb./hr. capacity, coupled with two 33,000-kW. hydrogen-cooled generators. Plant layouts and details are given of the boilers, economizers, air preheaters, turbines, condensers, generators, boiler feed pumps, condensate pumps, induced-draught fans, forced-draught fans, feed-water system, steam, salt-water pumps, fuels fired, steam flows, combustion control systems, soot blowers, and dust collectors.—M. D. J. B.

Operating Practice for Electromagnets Used for Lifting Purposes. E. Horoszko. (Hutnik, 1950, vol. 17, July–Aug.,

pp. 235–238). [In Polish]. General characteristics of electromagnetic cranes, the influence of operating conditions, their efficiency, and new designs are described.—V. G.

Design and Maintenance of Crane Runways. V. L. Smith. (Iron and Steel Engineer, 1950, vol. 27, Aug., pp. 67–70). When designing crane runways the primary factors to be considered are: The use to which the runway is subjected; whether heavy, medium, or light; the direction of loads; and the reaction to loads. The author stresses the importance of examining these factors very carefully if excessive rail wear and damage to the structure, the gantries, and the cranes are to be avoided. Rails and rail fastenings, types of runway, maintenance problems, design details including design of mill building columns, and costs are examined.—M. D. J. B.

A Static Magnetic Amplifier for Regulating Circuits. H. E. Larson. (Iron and Steel Engineer, 1950, vol. 27, July, pp. 88–101). The development of a self-saturating, static, magnetic amplifier is described. The advantages claimed are: No moving parts, no starting delay, sturdiness, and long life. Operating conditions and circuit diagrams of the amplistat as well as speed of response, input-output characteristic, amplification factor, effects of variations in A.C. supply voltage and frequency, and in load resistance are discussed. The various uses of the instrument are described including an adjustable-voltage speed variator drive, voltage regulators for power rectifiers, a current-tension regulator for the winding reel of a 5-stand tandem cold strip mill, and a regulating system for control of strip catenary in a pickling tank.—M. D. J. B.

Maintenance of Electric-Motor Control Gear. J. L. Watts. (Sheet Metal Industries, 1950, vol. 27, Feb., pp. 128–129; Mar., pp. 218–220). Recommendations on the inspection and maintenance of circuit breakers, starters, contactors, sequence devices, under-voltage and over-current releases, and fuses are made.—R. A. R.

Report of A.I.S.E. Wiring Committee. C. G. Dimitt. (Iron and Steel Engineer, 1950, vol. 27, July, pp. 53–77). This report outlines the research work and technical studies carried out by the Iron and Steel Engineers' Crane Wiring Committee on steelworks cranes. These have been classed into average and heavy-duty, based on the number of hours' operation per year, percent time in operation, number of moves per hour, temperature, and a number of other factors. The report covers conductor cable ratings, motor rating, both continuous and limited time ratings; mill-motor frame data, basic data, and proposed standards, as well as considerable information on test data.—M. D. J. B.

A.I.S.E. Standard No. 8 (Tentative)—Crane Wiring Standards, October, 1949. (Iron and Steel Engineer, 1950, vol. 27, June, pp. 115–119). This is a tentative standard prepared by the Association of Iron and Steel Engineers' Crane Wiring Committee under the chairmanship of J. E. Bodoh and C. G. Dimitt for circulation among members in order to obtain comments and criticisms before final publication. The standard covers the classification of crane service, wiring of A.I.S.E. D.C. mill motors and miscellaneous D.C. motors, conductor sizes, and conductor insulation.—M. D. J. B.

Coil Handling in the Steel Industry. D. A. McArthur. (Iron and Steel Engineer, 1950, vol. 27, May, pp. 59–75). This paper deals only with the handling of wide coils in wide strip mills and contains information obtained from 25 of the largest mills. Considerable statistical information is given as well as flow diagrams of materials, data on the stocking of hot-rolled coils for sheet and tin-mill products, pickled coils, cold coils ahead of cleaning line and ahead of annealing, etc. The practice of various mills in handling coils is given in chart form. Broadly, coils can be handled by cranes, tractors, conveyors, ramps, and cars. These methods are reviewed and the relative merits considered. The paper is illustrated with numerous diagrams and photographs.

M. D. J. B.

Materials Handling in a Steel Plant. L. R. Astie. (Iron and Steel Engineer, 1950, vol. 27, May, pp. 51–58). The importance of materials handling in the production of steel is discussed. Forty percent of the labour on the pay-roll in the average steelplant is involved directly or indirectly in the handling of materials, representing about one-third of the total cost of production. The general problems and accomplishments in mechanical handling are reviewed and the tonnages involved in intra-plant movements given. The need for systematic study of materials-handling methods is stressed and a review of new methods considered. The economics of handling materials are also discussed. Examples are given of the handling of bricks to furnaces, coal arrivals, the removal of debris from open-hearth furnaces, self-loading lorries, the uses of portable conveyors and other types of equipment.

M. D. J. B.

Handling Equipment for the Finished Products Stockyard of the N. V. Hollandsche Constructiewerkplaatsen at Leida (Holland). (Costruzioni Metalliche, 1949, vol. 1, Sept.–Oct., pp. 15–17). A description is given of an electric overhead travelling crane covering a semi-circular stockyard. The crane has a 66-ft. span and a capacity of 5 tons. The gantries are 33 ft. high and nearly 600 ft. long. The crane serves an area of approximately 4400 sq. yd., capable of stocking 8000 tons of rolled steel. The semicircular runway was adopted in order to make the best use of the available ground. Details are given of the various problems encountered in this unusual layout and of how these have been overcome.—M. D. J. B.

LUBRICANTS AND LUBRICATION

Spray Lubrication of Open Gearing. F. L. McCallum. (Iron and Steel Engineer, 1950, vol. 27, Sept., pp. 118–119). The properties of lubricants suitable for spray-gun application to open bearings as well as the different types of spray gun are discussed.—M. D. J. B.

Maintenance of Oil Circulating Systems. T. A. Marshall. (Iron and Steel Engineer, 1950, vol. 27, Sept., pp. 116–117). This paper deals briefly with circulating systems supplying oil to a number of units from one central station. Stress is laid on the necessity of training men to install, maintain, and service modern lubrication equipment.—M. D. J. B.

Evolution and Analysis of Drawing Lubricants. G. A. Cairns. (Steel Processing, 1950, vol. 36, Mar., pp. 135–137, 149). The author describes how animal fats and mineral oils have been developed singly and in combination, to produce present-day drawing lubricants. He then deals with the advantages and disadvantages of compounds used in the porcelain enamelling industry. These include straight or completely saponified soaps, soluble oils, fatty emulsions, plasticized resin compounds, and dried soap compounds.

P. M. C.

Application and Use of Lubricant Coatings. F. M. Hauger. (Wire and Wire Products, 1950, vol. 25, July, pp. 579–581, 610). Reference is made to a new wire-drawing compound, marketed by Standard Industrial Compounds Company of Chicago. There is no clue to the nature of this compound, which goes under the name of 'lubricant coating,' except that it is a paste containing no lime, yet requiring no previous coating of the wire, and that it is removed by hot water and very dilute acid.—J. G. W.

New Principle for Studying the Effectiveness of Lubricants in the Working of Metals by Pressure. S. Ya. Veiler and L. A. Shreiner. (Doklady Akademii Nauk, S.S.S.R., 1949, vol. 68, No. 2, pp. 325–328). The authors described a simple and convenient method for obtaining a quantitative value of the efficiency of lubricants used when drawing metals. The tangential forces arising when a steel ball is pushed through a cylindrical hole in metal by a punch are measured and curves for drawing force against punch travel, for the dry and lubricated conditions, are plotted and compared.—R. A. R.

Boundary Lubrication of Steel. W. Davey. (Industrial and Engineering Chemistry, 1950, vol. 42, Sept., pp. 1837–1841). Boundary lubrication of steels by blends of acids, esters, and soaps in mineral oil has been studied by means of the four-ball machine. Acid blends reduce friction by physical adsorption of the acid, and reduce wear by formation of soap films of low shear strength. Esters are less effective in wear reduction since soap formation can occur only under very extreme conditions. Metal soap blends are most effective in boundary lubrication if metal interchange between soap and iron can occur. Addition of sulphur markedly improves the lubricating properties of acid, ester, and soap blends; sulphide films, which facilitate absorption of polar compounds at the surface, are formed, giving enhanced anti-seizure properties.—C. J. B. F.

WELDING AND FLAME-CUTTING

Weldability of Plain Carbon and Low Alloy Steels. E. B. Suters. (Australian Institute of Metals: Australasian Engineer, 1950, Apr. 6, pp. 35–44). Four factors influence weldability and the occurrence of cold cracking. These are:

Absorbed hydrogen, chemical composition and structure of the base metal, cooling rate after welding, and stresses in the heat-affected zone. Hydrogen, which is often generated by the combustible ingredients in the electrode coatings, promotes cracking when trapped below the weld metal. This trouble can, however, be eliminated by the use of lime-ferritic electrodes, or the effect minimized by using austenitic electrodes. Weld hardening becomes excessive when the carbon content rises above 0·30%, and it is therefore preferable to increase the tensile strength, when necessary, by increasing the alloy content and not the carbon. 'Banding' also promotes cracking, whilst titanium and zirconium are useful inhibitors. Slow cooling can be produced by increasing electrode size and current input, slowing down the welding speed, and by preheating or post-heating. Stresses in the heat-affected zone will always be present, but can be reduced by proper welding sequence. Eleven recognized tests for weldability are described and illustrated.—P. M. C.

Some Metallurgical Observations of the Welding Processes. E. J. Raymond. (Australian Institute of Metals: Australasian Engineer, 1950, June 7, pp. 53–58). A short outline is given of the progress made in welding techniques during the past half century, and the following oxy-acetylene processes are described: (*a*) Fusion or liquid-phase welding which may be accomplished with or without filler metal; (*b*) non-fusion or liquid-solid phase welding, in which the parent metals are joined by an alloy of lower melting point; and (*c*) pressure or solid-phase welding, in which the two parts are heated to just below their melting point, and then upset one against the other. The application of these methods to steel, cast iron, and aluminium is outlined, and the author concludes with a short description of the argon arc process.—P. M. C.

Fatigue of Structural Joints. W. M. Wilson. (Adams Lecture: Welding Journal, 1950, vol. 29, Mar., pp. 204–210). The use of arc welding as a means of fabricating steel structures is described. Fatigue test results obtained on a number of welds on some rolled I-beams are quoted.—U. E.

The Role of Inert Gases in Electric Arc Welding. K. W. Winsor. (Australian Institute of Metals: Australasian Engineer, 1950, May 8, pp. 35–44). The author describes the use of argon in the arc welding process. The arc, which is struck between the work and a virtually non-consumable tungsten electrode, operates within an envelope of inert argon gas. In consequence there is no oxidation of elements and no porosity. Fluxing materials are not required and post weld cleaning is obviated. The method enables the satisfactory welding of aluminium, magnesium, and other materials which, before its development, were regarded as 'difficult-to-weld' metals to be carried out. The process can be easily mechanized, and may be operated with unskilled labour. A list of 134 references is included.—P. M. C.

'Heliarc' Welding in the Automotive Industry. R. H. Bennewitz and F. J. Pilia. (Welding Journal, 1950, vol. 29, Feb., pp. 126–136). The Heliarc process as applied to automotive industry is described. Argon gas is used as a shield during welding. A filler metal such as a chromium–manganese–silicon rod, and a torch angle of about 15° to the work are generally used. The electrode extension should not be greater than $\frac{1}{2}$ in., preferably $\frac{1}{4}$–$\frac{3}{8}$ in. For welding 0·05 in. automotive sheet, the recommended gas flow is 4–5 l./min. of argon. A current range of 110–160 amp. D.C. will weld satisfactorily a thickness of 0·037–0·05 in. A welding speed of 16–40 in./min. can be realized in Heliarc hand welding.

U. E.

The Necessity for Rationalization and Mechanization in Welding. J. Pilarczyk. (Przeglad Techniczny, 1950, July–Aug., pp. 385–388). [In Polish]. Some auxiliary installations for hand welding and some automatic welders are described.—V. G.

Application of Mild Steel Electrodes. F. R. Strate and R. T. Strough. (Welding Journal, 1950, vol. 29, Mar., pp. 193–198). The classification of electrodes is discussed. Weld metal of the highest average mechanical properties is deposited by electrodes E6010, E6011, E6020, and E6030. E6010 or E6011 electrodes can be used for filling up a crack formed in the first pass when welding heavy sections with E6020 electrodes. E6010–6013 are suitable for welding sheet metal.—U. E.

Nickel–Molybdenum–Vanadium Alloy Steel Shielded Arc Welding Electrodes (Low Hydrogen Type). E. H. Franks, C. T. Gayley, and W. H. Wooding. (Journal of the American Society of Naval Engineers, Inc., 1950, vol. 62, Aug., pp. 601–619). Investigations by the American Navy authorities in the development of a nickel–molybdenum–vanadium electrode for welding low-alloy air-hardening steels are reported. Its welding characteristics are compared with those of 25/20 austenitic electrodes. Moisture in the coating had a marked effect in promoting weld metal cracking and maximum moisture was specified at 0·20%. When electrodes are exposed in humid atmospheres the coatings absorb moisture very rapidly. The electrodes are baked at 800–900° F. before packing by the manufacturer. The strength and elongation of weld metal deposited with this electrode under different conditions are given by a series of curves.—R. A. R.

Steel Welding Wire. G. E. Claussen and H. E. Sincock. (Wire and Wire Products, 1950, vol. 25, Oct., pp. 841–843, 846, 847, 908–911). The effect of the quality of the core wire, particularly its composition, on the performance of coated welding electrodes has been investigated. The experiments were confined to low-carbon and low-alloy steels. In general, the sulphur should be as low as possible and, in the case of rimming steels, segregated areas in the ingot should be excluded from the wire. The surface of the wire should be chemically clean to ensure good adhesion of the extruded electrode coating. Among the heavily coated electrodes some are more sensitive than others with respect to cleanliness and composition of the core wire. Changes in carbon, manganese, silicon, and sulphur in the core wire have decisive effects on porosity and cracking of the weld. For light-coated electrodes the effect of wire composition and welding performance can be predicted from the welding behaviour of the bare core wire. Tests revealed a dependence of the tendency to burnt welds on the composition of the wire and on the lubricant used in the drawing process.—V. G. W.

Applications of Unionmelt Welding. C. A. Heffernon. (Canadian Metals, 1950, vol. 13, Aug., pp. 32–34, 48). This process is characterized by the heavy welding current, and a granulated welding flux excluding air from the welding zone on melting. The rod feed is regulated by the welding voltage. Welding-rod materials and welding compositions are available for most steels and non-ferrous metals and alloys, except magnesium and aluminium. Joint design and preparation are described as important, and examples are given. Progressive and stationary mounting of the welding head, also mechanized shape welding, manual and continuous mill operation, and building-up surfaces are exemplified.—E. C.

Design and Manufacture of Steel Tubes. J. S. Blair. (Journal of the Institution of Heating and Ventilating Engineers, 1950, vol. 18, Nov.–Dec., pp. 328–365). The first part of the paper gives a detailed account of the manufacture of welded and seamless tubes. The processes dealt with are continuous weld, lap weld, hydraulic weld, electrical resistance weld, plug mill, push bench, and pilger mill. The finishing processes of cold drawing and hot reducing are also described. The relative merits of the different types of tube, the reasons for the large variety of processes and different tolerances in diameter and thickness are discussed. The reasons for the difficulty in specifying bore tolerances are given. The second part deals with the determination of the necessary diameter and thickness for various conditions, great attention being given to steam pipes conforming to British Standard 806. The last part reviews briefly the principal types of joint.—R. A. R.

From Steel Plate to 16 Inch Pipe. W. A. Connor. (Canadian Metals, 1950, vol. 13, Apr., pp. 28–30). An electric-resistance-weld pipe mill newly installed at Page-Hersey Tubes, Ltd., Welland, Ontario, is described. The mill can produce steel pipes from 4·5 in. in dia. and 0·337 in. maximum wall thickness up to 16 in. in dia. and 0·500 in. wall thickness in lengths up to 50 ft. Production is continuous at a speed ranging from 15 to 60 ft./min.—E. C.

Magnaflux Inspection of Welded Storage Tanks. F. A. Upson. (Welding Journal, 1950, vol. 29, Jan., pp. 27–30). The Magnaflux test suitable for testing all-welded construction is described. Magnaflux powder is applied to the surface of the weld and a magnetizing current is passed through the weld metal in successive overlapping steps between heavy copper probes which are held in contact with the weld. Defects such as slag stringers, cracks, lack of fusion, and poor

penetration elongated in the direction of the weld seam, provided they do not lie too far below the surface, are shown up by the accumulation of powder on the surface—U. E.

The Welding of Pressure Vessels. H. Harris. (West of Scotland Iron and Steel Institute, Nov., 1950, Preprint). The present stage of development in the manufacture of welded pressure vessels is described, with special reference to the plant and practice of Babcock and Wilcox, Ltd. The problems involved by the necessity for using heavy boiler plates are discussed, and a brief description of the inspection procedure is given. Data substantiate the claim that welding has now attained a very high standard of quality and several factors responsible for this position are reviewed. Reference is made to the problems that have arisen in the final inspection of the welded seams and the results of an investigation on the utility of supersonic examination are revealed.—P. C. P.

Electrodes for Spot Welding Galvanized Steel. H. B. Spore. (Welding Journal, 1950, vol. 29, Jan., pp. 31s–36s). A range of machine settings for spot welding galvanized steel is recommended. The influence of tip pressure between limits of 40 and 90 lb./sq. in. is unimportant. The effect of tip pressure on indentation for various welding currents for all weld periods for 2 in. radius of tip was investigated and minimum values were obtained at the middle of the range of tip pressure. The weld strength does not increase linearly with weld current. Moderate welding currents should be used when welding 22 gauge galvanized sheet, as excessive indentation values are obtained at high welding currents. A weld period of 3–7 cycles is optimum for 2-in. and 6-in. radius tips. The 10-in. radius takes an 11-cycle weld time satisfactorily. Slightly heavier welding current or longer weld time is necessary with 1·7-oz. coated material than with the 1·0-oz. coating to obtain a satisfactory weld strength.

U. E.

Speed and Uniformity in Joining Steel Provided by Contact Welding. T. C. Du Mond. (Materials and Methods, 1950, vol. 32, July, pp. 55–57). In this recently developed process, the electrode is kept in contact with the work at all times. Special electrodes are used, the coatings of which contain steel powder. This higher cost is offset by the increased speed of welding and by the heavier deposits, thus saving time and labour. The application of the process to spot welding is described, and the low nitrogen content of the weld metal is compared with that of normal arc weld deposits.—P. M. C.

Resistance Welding of Stainless Steels. G.-A. Herpol. (Pratique du Soudage, 1949, vol. 3, Dec., pp. 185–188; 1950, vol. 4, Jan., pp. 6–19; Feb., pp. 25–26; Mar., pp. 40–42). In Part I the author discusses the weldability of stainless steels and in particular the 18/8 alloy. These steels have a carbon content below 0·2% and get their austenitic characteristics by high-temperature heat-treatment and rapid quenching. With resistance welding a very similar process occurs; the steel is raised locally to a temperature above transformation temperature and the rapid cooling of the weld increases the austenitic character of the steel with corresponding improvement of mechanical properties. The importance of speed in welding in order to restrict the precipitation of carbon and chromium which would compromise the corrosion resistance of the steel is stressed. The electric and magnetic properties of nickel–chromium steels, which are favourable to resistance welding, are also considered. In Part II the author discusses in detail resistance spot-welding procedure, the electric current, the diameter of electrodes, contact pressure of the electrodes, and welding time. In Part III similar information is given for continuous strip welding. Part IV examines in detail the influence of plate thickness on resistance welds.

M. D. J. B.

New Flash Butt-Welder Joins ¼-Inch Strip in 10 Seconds. D. Reebel. (Steel, 1950, vol. 126, May 8, pp. 85–86). This article describes a new 60-in. 600-kVA. strip welder to be installed at the Forge Aciéries Unisor steel mill outside Paris. Features are automatic hydraulic welding control, the main die units can be changed quickly, and there is an improved gauging system.—A. M. F.

Notable Welded Structures Erected in the Last Ten Years. (Costruzioni Metalliche, 1949, vol. 1, July–Aug., pp. 18–21; Sept.–Oct., pp. 10–14). Descriptions are given of the more remarkable welded structures erected in the last ten years. First of these is the new bridge over the Seine at Neuilly. This has two flat arches of 67 and 82 m. span and a total weight of 2240 tons. Thirty-three tons of electrodes were used constituting 66,000 in. of welding of which only 9000 in. were welded on the site, the rest being shop welded. The bridge is of high-tensile steel and necessitated considerable scientific research as this was the first time a similar structure was erected in high-tensile steel. The main girders are of tubular construction and very pleasing design. A second bridge over the Seine near Saint Cloud is also described. This bridge is 190 m. long 30 m. wide and the heights of the main girders vary between 1·74 and 1·52 m. The bridge has seven horizontal girder spans. The well-known Hawkesbury bridge in New South Wales is also described.—M. D. J. B.

Modifications to Factory Roof by Welding. (Costruzioni Metalliche, 1949, vol. 1, Sept.–Oct., p. 7). A description is given of the rebuilding of a factory roof using the original stanchions. The conventional-type truss of the old roof was replaced by a monitor roof giving greater headroom, less obstruction, and better lighting and ventilation. The modification was made possible by electric arc welding.

M. D. J. B.

BOOK NOTICES

ANDREWS, C. REGINALD. "*The Story of Wortley Ironworks.*" 8vo, pp. 80. Illustrated. Mexborough, Yorks., 1950: The South Yorkshire Times Printing Company, Limited. (Price 7*s.* 6*d.*)

This well-illustrated book written by the former vicar of Wortley, is partly based on personal reminiscences. Part I begins with a lively description of the ironworks as they were about 50 years ago. The history of the works is traced from the Middle Ages until the closing of the works in 1929. There is certainly much to be said for the author's presumption that the ironmaking activities at Wortley owe their beginning to the Cistercian monks who mined and worked iron in adjacent districts commencing with the second half of the XIIth century, but this is not conclusive by a long way, nor should too much significance be attributed to the frequent occurrence of the family name of 'Smyth' in the Poll Tax Returns of 1397, as by that time the name had no longer any occupational significance. This consideration makes it rather doubtful whether the occurrence of this name in connection with Wortley in 1397 can be taken as "the first definite historical evidence of the existence of the works" (p. 11). The first documentary evidence of Wortley Ironworks is a lease of 1621 discovered by the author among the Earl of Wharncliffe's records preserved at Wortley Hall. The existence of large beds of bloomery cinders in the vicinity of the forges appears to indicate a bloomery dating further back, but whether it was of medieval or of later origin cannot be ascertained as the bloomery process remained practically the same for many centuries. The last chapter (X) of Part I deals with Rockley furnace erected in 1652. Of particular interest among the illustrations is a photostat taken from accounts of bar iron and rod iron manufactured in 1696–97 (Plate III) and the old Wortley hammer stone of 1713 (Plate IV) with a typical helve hammer of that period carved in stone.

Part II "Odds and Ends," completes the story of the works by interesting details from the past, such as a description of the Tin Mill built in 1743, of an old iron wain, still in use in 1908, and notes on chain-making at Wortley introduced from Staffordshire.

The Story of Wortley Ironworks is a delightfully written and interesting history of a remote works in Yorkshire. It is to be hoped that Wortley Forge will be established as a permanent industrial museum, as is planned by the Sheffield Trades Historical Society.

H. R. SCHUBERT.

DEDERICHS, R. AND H. KOSTRON. "*Zwei neue Schnellverfahren zur Kornquerschnittbestimmung.*" F'scap, pp. 31. Illustrated. Weinheim/Bergstrasse 1950: Verlag Chemie G.m.b.H.. (Price DM 14.–)

This pamphlet should prove of extreme interest and

practical value to those concerned with grain-size estimation. The word estimation is stressed, for there are a number of sources of error inherent in any measurement of such a quantity as grain area. The subject matter has been reprinted from *Archiv für Metallkunde* and this presumably is some indication of its implied value.

The first section of the text deals with the basic assumptions which are made in attempting to assess grain size, and from experimental observations and statistical considerations the authors proceed to define the conditions under which the most reliable estimation of grain size can be expected. The reasons for preferring, in general, the use of a logarithmic mean rather than an arithmetic mean area are dealt with at length. It is not essential that this section be read in order to use the proposed methods, but since it does clearly describe the possible sources of error and their effect on accuracy, its reading is strongly to be recommended.

The second section explains the principles on which the two procedures are based and shows the connection between the 'grain numbers' so obtained and the more conventional A.S.T.M. numbers. The first method, for estimating mean grain size, assumes that the section of metal photographed is of essentially uniform nature with respect to actual grain size and to size distribution, but *not* that the exposed grain faces are roughly circular or hexagonal in shape. In essence, the authors have prepared a series of synthetic photomicrographs in which the mean grain size of one photograph is 1·78 times that of the preceding one. The factor 1·78 was chosen because it is $10^{\frac{1}{4}}$, a convenient mantissa, and because experience has shown that the naked eye can readily distinguish between two photographs in which the grain sizes are in that ratio. One such series (*M*1) of photographs is made to correspond to the magnification at which the actual specimen is viewed or photographed, of 1, 10, 100 and 1000, another (*M*2) to a magnification of 2, 20, 200, and 2000, and a third (*M*5) to 5, 50, 500, and 5000. Each such series is subdivided into groups in which the ratio of the longest dimension of the grains to the shortest is 1, 2, 4 or 8. The pages on which these synthetic microsections are reproduced are thus labelled *M*1, $S = 1$; *M*1, $S = 2$; *M*1, $S = 4$; ... *M*2, $S = 1$; *M*2, $S = 2$; ... *M*5, $S = 1$, etc. The microsections have been prepared by degrading appropriate 'brickwork' structures.

For an estimation, the metal section is viewed at one of the above magnifications and the group *M*1, *M*2, or *M*5 is selected accordingly. Comparison of the real with the synthetic grains will indicate which *S* factor applies and the appropriate page is removed from the loose-leaf folder. The picture, one of eight on the page, which corresponds most closely to the viewed specimen is noted and by reference to adjacent tables the value of the mean grain area, *F* (in μ^2), the grain number *M* (= log*F* to the nearest $\frac{1}{8}$) and the corresponding A.S.T.M. number can be found. The grain size is then characterized as, *e.g.*, *F* 75μ^2, *S* 2 or *M* $1\frac{7}{8}$, *S* 2.

The second method is for determining the size of individual grains. For this purpose, three stout transparent plastic sheets are provided to lay over the specimen or a photograph thereof. Again the sheets, *R*1, *R*2, and *R*5 refer to the magnifications of $1 \times 10^{\mathbf{x}}$, $2 \times 10^{\mathbf{x}}$, and $5 \times 10^{\mathbf{x}}$ as above. Each sheet carries a series of rectangles whose areas are 1·78 times that of the preceding one and there is a series for each *S* factor of 1, 2, 4, 8, and 16. From the known magnification at which the grain is being viewed (one of the above), the appropriate sheet is selected and, by inspection, the correct *S* group chosen. These rectangles are laid in turn over the grain to be measured and one is found whose area is the same as that of the grain. Reference to the inscribed tables then gives the value of the grain size *F* or grain number *R* (= log *F*). The grain is described as *e.g.*, *F* 1330μ^2, *S* 4 or *R* $3\frac{1}{8}$, *S* 4.

The price of the pamphlet, about 24*s.* is considered reasonable and should soon be recouped by the use of the pictorial pages and plastic sheets.—J. Pearson.

Kubasta, J. "*Das Härtungsverhalten der Edelstähle.*" Zweite Auflage. 8vo, pp. v + 204. Illustrated. Halle (Salle), 1949. Verlag Wilhelm Knapp. (Price: unbound DM 12.50; bound DM 14.20).

For the development of high-quality steels, a detailed understanding of hardening properties is of equal importance to a knowledge of chemical compositions and correct applications.

The purpose of this book is to explain the theory of hardening characteristics on the basis of the behaviour of individual special steels and to suggest ways of applying this theory. The thermodynamic laws relevant to heat-treatment must be applied to explain the hardening behaviour.

The author's conception of the 'transformation limit,' based on the maximum diameter to which the steel can be hardened throughout, was included in the special German Reichstelle Order E23b laying down the fields of application of structural steels according to their hardenability. The Verein deutscher Eisenhüttenleute Materials Specification 150–47 on the hardening behaviour of tool steels also contains the author's data for the measurement of depth and penetration hardening normally to be expected by the consumer.

After discussing the factors affecting hardening properties and the conception of 'abnormal hardening' behaviour, the author describes methods of representing graphically various fracture appearances. The development of a numerical expression of entropy and the inherent constant urge in a metal to transform to a final state are discussed in relation to hardening. The hardening processes of carbon steels are described and the relation between heat-treatment and changes in transformation is surveyed.

The following factors are studied: Influence of period of heating on hardening temperature; effect of the number of quenches on the appearance of the fracture; influence of preheating to a temperature above the hardening temperature, and of the time of cooling from that temperature to the hardening temperature; quenching media of various intensities; repeated hardening at various temperatures; normalizing before hardening; full annealing; stress relieving; influence of surface finish on hardenability; salt bath hardening; and effect of hardening and tempering before final hardening.

Chapters 6, 7, and 8 discuss relationships between hardness stresses, hardness cracks, and structural transformations, and between grain size and transformations.

Subsequent chapters discuss gas contents, chemical reactions, and melting processes in relation to hardening behaviour. The theory and application of hardenability specifications as a complement to analytical data are explained in relation to some modern tool and structural steels.

Chapter 15 discusses the application of hardenability specifications to the following special hardening processes: (1) Austempering; (2) case-hardening; (3) intermediate stage hardening (a single, continuous hardening and tempering); (4) hysteresis hardening (steels with sluggish transformations have their Ar_1 point considerably lower than their Ac_1 point. After heating above Ac_1 (= about 800° C.) in the furnace, parts from such steels are cooled slowly and uniformly to a temperature slightly above their Ar_1 point, *e.g.*, 710° C. and then oil-hardened); (5) surface hardening (local heating by flame or induction current); (6) emulsion hardening; (7) salt bath hardening; (8) primary hardening from hot-working temperature; and (9) sub-zero quenching.

Finally, the author relates the application of hardenability specifications to the production of special steels and scientific research.—R. Sewell.

Nikuradse, A. and R. Ulbrich. "*Das Zweistoffsystem Gas–Metall. Physikalisches Verhalten.*" 8vo, pp. 199. Illustrated. Munich, 1950: R. Oldenbourg Verlag. (Price DM 16.–)

The importance of gases in, and on the surface of, metals in influencing mechanical and electrical properties is becoming increasingly realized. A monograph which sets out to survey the results of investigations into the physical interaction of gases and metals should therefore be welcome. This publication is conceived on an admirable plan, though unfortunately its execution leaves something to be desired. The authors devote the first 25 pages to

a discussion of the Coulomb, and van der Waals' and valence forces such as arise between a metal surface and its external environment. The treatment is interesting but suffers from the defect of having assumed too much or too little knowledge on the part of the reader. For example, those with no previous acquaintance with the wave-mechanical concepts of interatomic and intermolecular forces are likely to be confused with the terminologies which are employed without explanation, whilst those to whom they are more familiar will find the treatment somewhat scanty. This probably comes from trying to compress so much into such a slender volume.

The second section, some 60 pages, is concerned with gases adsorbed on metal surfaces. It deals very thoroughly with the various types of isotherm observed experimentally and with the theoretical explanations advanced to account for them. A clear distinction is made between physical and activated or chemi-sorption, and the conditions under which each occurs are explained. After touching on the modern electron theory of metals, about 30 pages are devoted to the potential difference which exists in an adsorbed layer, the manner in which it arises, and the means by which it may be measured. The section closes with a short account of the effects of adsorbed gas layers in modifying the electrical resistance of thin films.

The importance of this section is emphasized by the next two, which deal with gases in metals, for the processes by which a gas enters or leaves the solid phase depend on activated surface adsorption and desorption. Much experimental work has been vitiated by neglecting this fact. The conditions which must be obeyed for a gas to dissolve and the various types of solution are described. Although it is true that most experimental work in this field has been devoted to solutions of hydrogen in metals, particularly palladium, it is to be regretted that the authors have not paid more attention to nitrogen and oxygen in iron and ferrous alloys. The section on diffusion suffers also from the defect of assuming too much or too little, and the diagrams to illustrate the Wagner-Schottky theory are not very helpful.

The final short section deals with permeability under conditions of silent discharge, though there seems no reason why this should not have been treated in the previous section where the effects of surface condition, gas pressure, and composition of the metal on diffusion and permeability are discussed; the problem is somewhat specialized and thus perhaps merits separation from the rest.

The reader is likely to be annoyed by the mistakes with which the monograph abounds. They seem to be evidence of a gross lack of care in preparing the original manuscript and in proof-reading. Mathematical expressions are the worst sufferers, though references to diagrams which do not exist or which are numbered incorrectly and the existence of illustrations which are not referred to in the text and which seem to have no direct connection therewith prove equally annoying. The publication includes a loose leaf of a mere eight errata but the insertion of one of the corrections in its appropriate place makes for even greater confusion. Nevertheless as a survey of a wide field, the monograph repays reading, though at a price of DM 16.– (about 26*s*.) one would be hesitant to purchase.

J. PEARSON.

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S. J. KENNETT, Ph.D.

STANLEY JOHN KENNETT was born at Ipswich on 5th May, 1906. He was educated at Northgate School, Ipswich, and at the Royal School of Mines, where he obtained his A.R.S.M. (First Class) in 1926 ; in the same year he was also awarded the B.Sc. (Eng.) degree of London University, with First Class Honours. During the next two years he undertook post-graduate study and research under the late Professor Sir Harold Carpenter, the work being concerned mainly with fusible alloys, including ageing phenomena. In 1927 he was awarded the Diploma of Imperial College and the Murchison Prize, and in 1930 he obtained the Ph.D. degree of London University.

In November, 1928, Dr. Kennett was appointed Assistant Lecturer in metallurgy at Liverpool University, under the late Professor C. O. Bannister. He became Lecturer in 1931 and then, ten years later, Acting Head of the Department of Metallurgy, being responsible for remodelling the degree courses in the direction of greater attention to physical metallurgy and to fabrication processes. In 1949 he visited a number of laboratories in the United States of America, a tour that was useful in connection with the planning of the new laboratories which were occupied by his Department in that year. Dr. Kennett's present research interests are precipitation phenomena in lead–tin alloys, the effect of precipitation upon creep behaviour, and the iron–chromium–copper system of alloys.

During the war, Dr. Kennett gave lectures to units of H.M. Forces in all three services ; among these activities was a lecture tour in Malta and Egypt, which he carried out for the Admiralty in 1945. This work was additional to the wide variety of industrial investigations carried out for various bodies during the war years.

Dr. Kennett played a leading part in the founding of the Liverpool Metallurgical Society and became its first President in 1948. He is active in the work of the International Student Service and was a member of the Assembly from 1946 to 1949. He is Chairman of the Liverpool Section of the Society of Chemical Industry, a Fellow of the Chemical Society, a Fellow of the Institution of Metallurgists, and a Member of the Institute of Metals. He was elected a Member of The Iron and Steel Institute in 1937, and became an Honorary Member of Council in 1950, during his tenure of the presidency of the Liverpool Metallurgical Society.

S. J. Kennett, Ph.D.

Honorary Member of Council, 1950